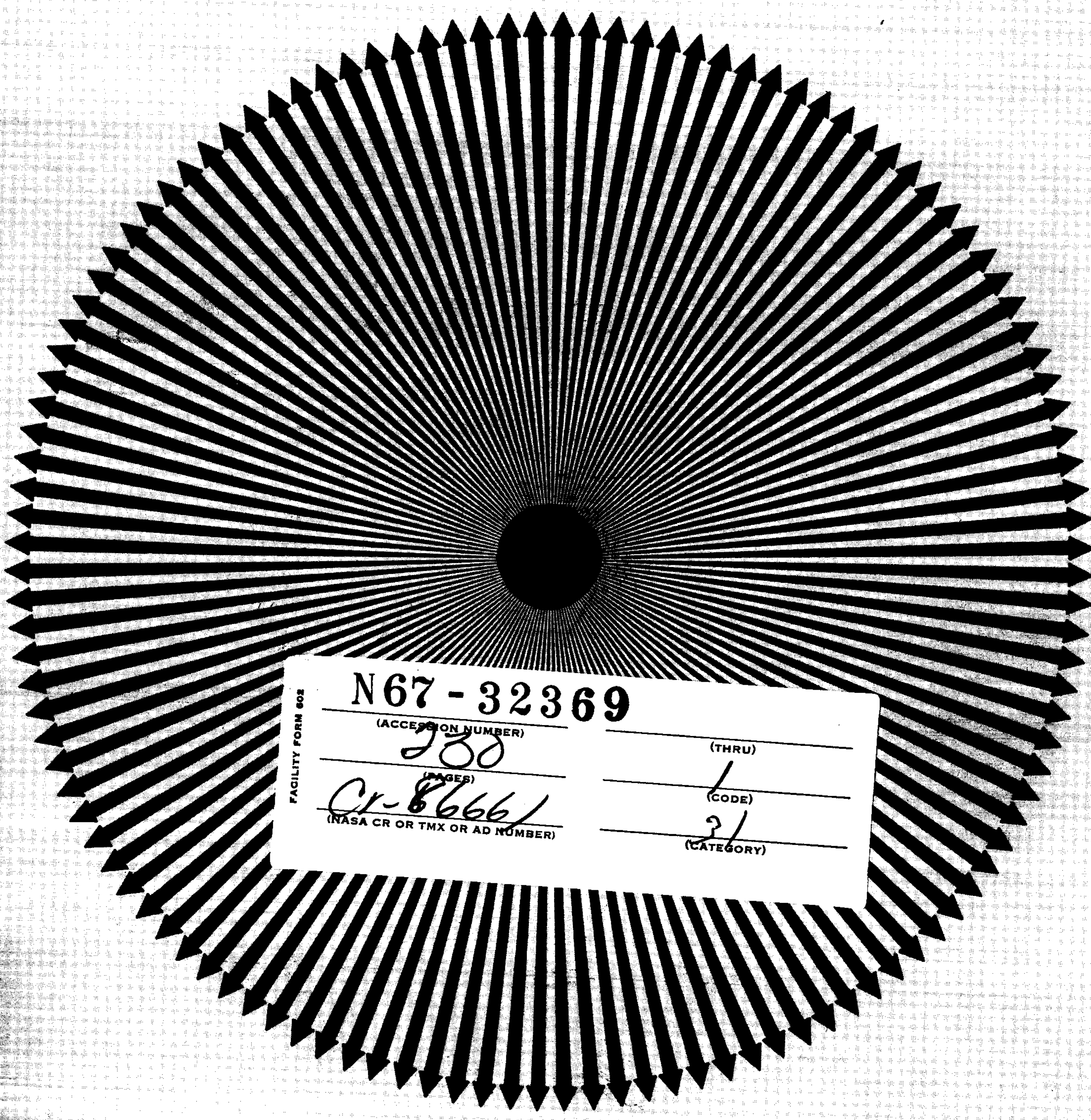
SPACECRAFT ATTITUDE CONTROL GAS SYSTEMS ANALYSIS



Final Report/SSD 70172R/JPL Contract No. 951720/April 1967
Propulsion and Power Systems Laboratory

HUGHES

HUGHES AIRCRAFT COMPANY
SPACE SYSTEMS DIVISION

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FINAL REPORT

SSD 70172R/JPL Contract No. 951720/April 1967

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CONTENTS

1. SUMMARY AND CONCLUSIONS

This report concerns comparative analyses of six candidate systems for execution of the attitude control and maneuvering requirements of a future unmanned planetary probe/lander vehicle. The systems compared were:

1) Stored cold gas (nitrogen and others)

2) Vaporizing liquid plenum (ammonia and others)

3) Hydrazine catalytic plenum (warmor ambient gas)

4) Water electrolysis plenum

5) Hydrazine electrolysis plenum

6) Dual mode hydrazine (hydrazine electrolysis plenum/hydrazine monopropellant thruster)

Because of the overriding significance of reliability in this mission, the propulsion systems were examined in ten different configurations specified by JPL and in four additional configurations, decided upon in the course of the study. The different configurations represent varying degrees of redundancy in propellant, tankage, and valving.

Three basic kinds of configurations were compared. The important features of the first (Series 1) are a single plenum containing the requisite amount of propellant to accomplish the mission and redundant sets of either quad or dual valves for control of propellant flow to the thrusters. The second (Series 2) is a parallel tank configuration in which duplicate plenums are employed, each with the amount of propellant necessary to complete the mission and each with either dual or quad valving. The two parallel systems are normally operated simultaneously, but may function singly upon command or in case of a major failure. The third configuration (Series 3), similar to existing Ranger and Mariner systems, is comprised of dual plenums, each containing 1.5 times the propellant required for the mission. The two plenums operate concurrently, with single valves. A fourth series, introduced as a result of the analyses, was composed of configurations in which a single plenum, with the requisite quantity of propellant, was used, with nonredundant quad and dual valve packages. Each of the configurations was also examined for the case of an additional thrust level and total impulse requirement for roll control during the major retro maneuver. The configurations are shown in Figures 1, 2, and 3 and summarized in Table 3. A 1972 operational date was assumed, and a technique devised to extrapolate reliability to that date.

Weights and reliabilities were estimated for the systems and configurations. With the exception of stored cold gas, none of the systems is current technology. It was concluded, however, that any of the systems could exhibit good reliability if the required development effort were conducted during the next 5 years. The two systems of greatest promise on the basis of weight and potential reliability proved to be the hydrazine catalytic plenum and the dual-mode hydrazine. In the former system, propulsion is provided by hydrazine decomposition gases stored in a plenum. A catalytic gas generator automatically supplies gas to the plenum. In the dual-mode hydrazine system, liquid hydrazine is electrolyzed and the resulting high specific impulse cold gas mixture is stored in the tank ullage space to provide attitude control pulses; conventional monopropellant thrusters are plumbed to the liquid storage portion of the same tankage for higher thrust maneuvering requirements.

The choice between the two best systems was different for the small and large total impulse requirements. For the small systems, the hydrazine catalytic plenum appeared better than the others according to combined weight and reliability considerations. In these systems, dual-mode hydrazine is satisfactory but slightly heavier than the catalytic plenum because an additional set of thrusters is needed. In the larger systems, the reliability of the catalytic plenum is degraded by a hot gas valve requirement and its weight is no longer the lowest; the dual-mode hydrazine system then becomes the most attractive.

The vaporjet is heavier than either of the two favored systems, but light enough to deserve careful consideration. It would appear to offer relatively little development risk, except possibly for the zero-g heat exchanger. In the present study, the design details were not investigated extensively, and only ammonia was considered for all of the alternative designs. In one such design, however, propane appeared to be better than ammonia, indicating that other propellants should be investigated further. The vaporjet is, in any event, promising as a backup program.

Neither stored cold gas, the water electrolysis plenum, nor the hydrazine electrolysis plenum is recommended for the mission studied. In all cases the tankage is excessively heavy.

The choice between configurations depended more on valve reliability than any other factor, except that weight might rule out the fully redundant two-plenum system (Series 2). Comparison of Series 4 and Series 3 results showed that quad or dual valves offer greater reliability and lower weight than the original dual-plenum, single-valve system used on Ranger and Mariner. It is notable that the reliability of any one plenum is higher with a single quad valve than with a pair of redundant quad valves because an open or leak failure is more probable than a closed failure and has twice as many paths through the redundant package. In all applicable cases, the recommended configuration is a pair of dual valves rather than one quad valve in order to minimize translation of the flight path from unbalanced torques. The slightly greater weight of the dual-valve thrusters compared with half the number of quad thrusters is not significant compared with total system weight.

It was also found that either of the two preferred systems – the hydrazine catalytic plenum and the dual-mode hydrazine – can be employed in a fully redundant two-plenum configuration with both weight and reliability advantages over the best nitrogen cold gas system, even if the latter were used with a single plenum. The preferred configuration is, then, two plenums, each employing a pair of dual-valve thrusters for each control function. If the larger total impulse requirement is imposed, the dual-mode hydrazine system is preferable; if the smaller total impulse requirement is satisfactory, the hydrazine catalytic plenum is selected.

2. INTRODUCTION

This report constitutes an analysis of six attitude control gas systems for spacecraft capable of executing an advanced space probe mission:

1) Stored cold gas (nitrogen and others)

2) Vaporizing liquid plenum (ammonia and others)

3) Hydrazine warm or ambient gas plenum

4) Water electrolysis plenum

5) Hydrazine electrolysis plenum

6) Hydrazine electrolysis plenum/hydrazine monopropellant thruster (the "dual mode" hydrazine system)

THE MISSION

The mission was to provide attitude control and maneuvering thrust for a space vehicle that could fly to a planet in 200 days, eject a capsule comparable in weight with the bus vehicle, and maintain orbit about the planet for an additional 200 days. During the flight, the vehicle was to perform midcourse corrections and other maneuvers similar to those executed by Ranger, Mariner, Surveyor, and other projected major space probes while maintaining referenced attitude by full three-axis control during the intervening cruise periods. A profile of the selected mission is shown in Table 1. In the analyses that follow the allotment shown under leakage and crosscoupling was distributed proportionally over the other events.

Thrust specifications are shown in Table 2. All systems were designed with adequate energy storage to deliver peak demands in one continuous firing at rated thrust; actual thrust level may be varied considerably without essential change in system design. Thrust is controlled within ±20 percent under all conditions of system operation.

TABLE 1. MISSION PROFILE

Event	Time	Duration	Impulse, lb-sec	
			No Roll Control	Roll Control
1) Launch (L)	0	-	-	-
2) Injection rate removal	L + 1 hr		170	170
3) Acquisition of references	L + 2 hr		20	20
4) Roll calibration	L + 2 hr	5 hr	340	340
5) Canopus acquisition	L + 7-1/2 hr	1/2 hr	15	15
6) Cruise	L + 8 hr	10 days	85	85
7) Commanded turns	L + 10 days		200	200
8) Midcourse correction	L + 10 days		0	100
9) Reacquisition of references	L + 10 days		10	10
10) Cruise	L + 10 days	190 days (from items 9 to 13)	1,200	1,200
11) Midcourse correction (second)	L + 30 days		0	50
12) Reacquisition of references	L + 30 days		10	10
13) Command turns	L + 200 days		500	500
14) Retro	L + 200 days	10 minutes	0	2,000
15) Reacquisition of references	L + 200 days		10	10
16) Orbital cruise	L + 200 days	5 days	30	30
17) Commanded turns	L + 205 days		160	160
18) Orbital trim	L + 205 days		0	50
19) Reacquisition of references	L + 205 days		5	5
20) Capsule separation	L + 205 days		0	0
21) Orbital cruise	L + 205 days	200 days	285	285
22) Leakage	Throughout - depends on pressure etc.		140	140
23) Crosscoupling	(Thrust misalignment)		420	420
Total required impulse	Single system two "dual" systems		3,600 10,800	5,800 17,400

THE SYSTEM CONFIGURATIONS

All systems were examined in each of 14 different configurations, as summarized in Table 3 and illustrated in Figures 1, 2, and 3. Ten configurations were selected prior to the study and the remaining four (4a, 4b, 4c, 4d) added as a result of the reliability analysis.

PLAN OF THE REPORT

The first portion of the report is intended to be a concise summary of results, conclusions, and recommendations. Details of the supporting analytical and survey material will be found in a series of appendices. Features of tankage, valves, and nozzles which are common to all systems are treated in the discussion of the baseline nitrogen cold gas system.

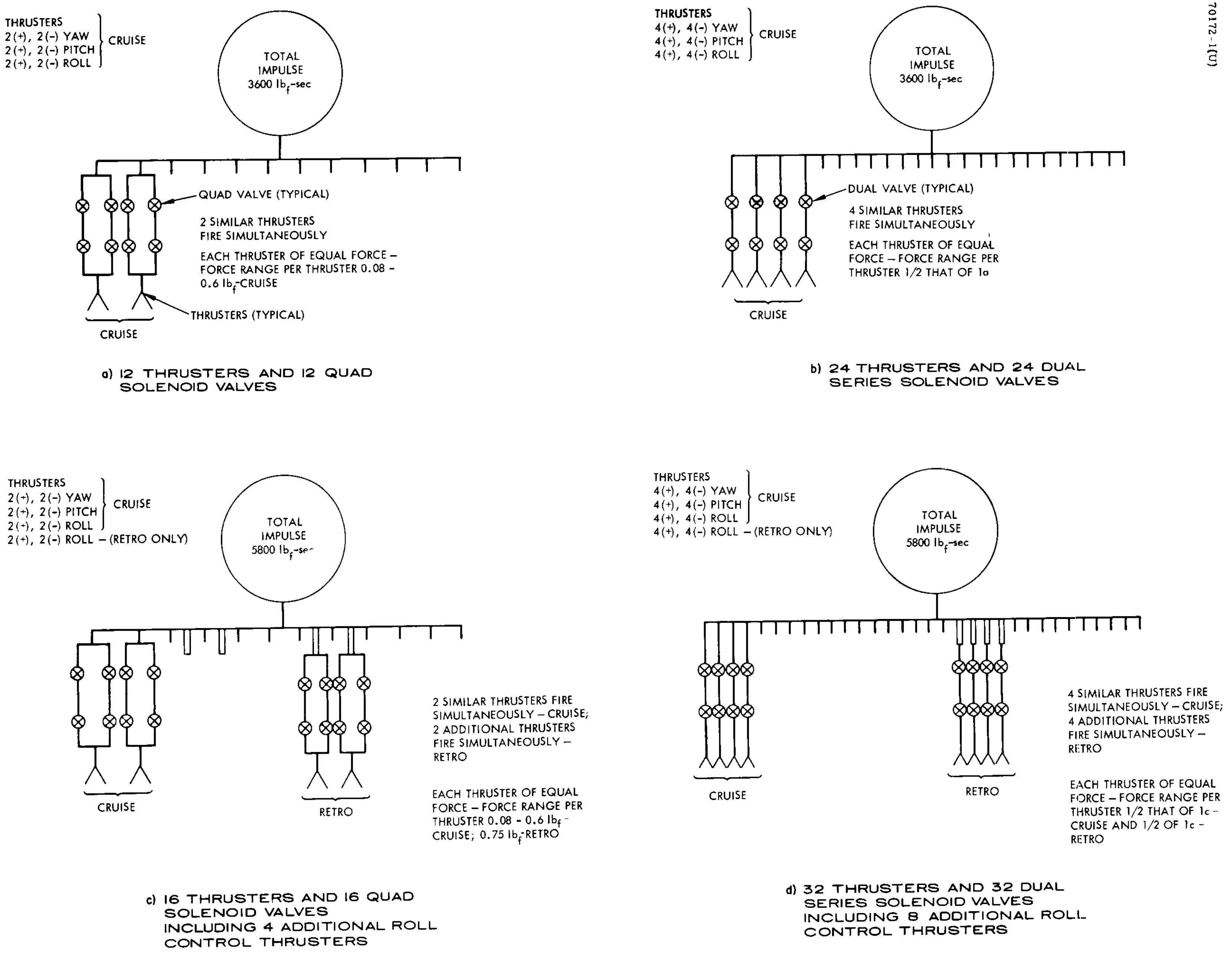


Figure 1. Single Tank System Configuration

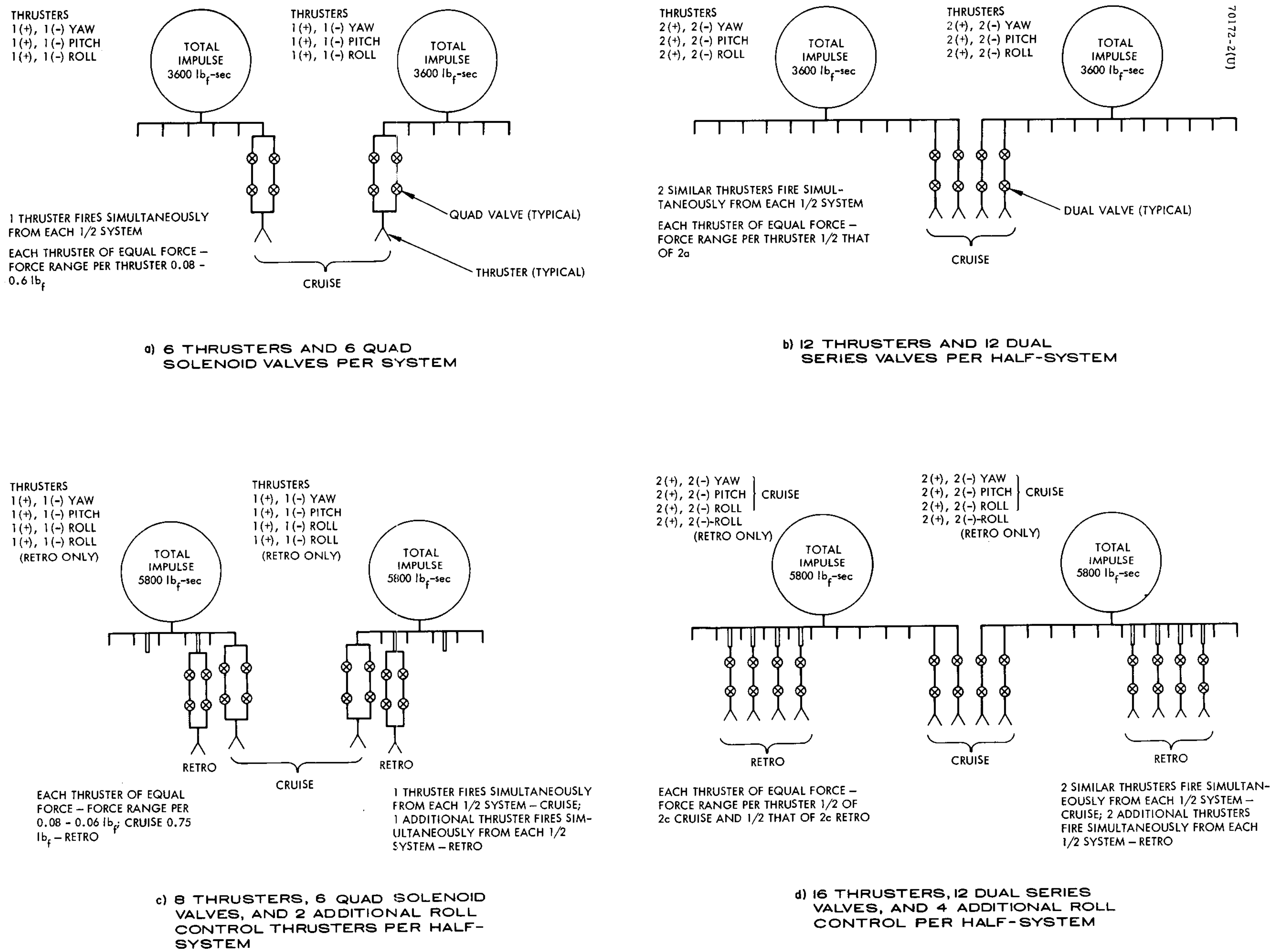


Figure 2. Parallel Tank System Configuration

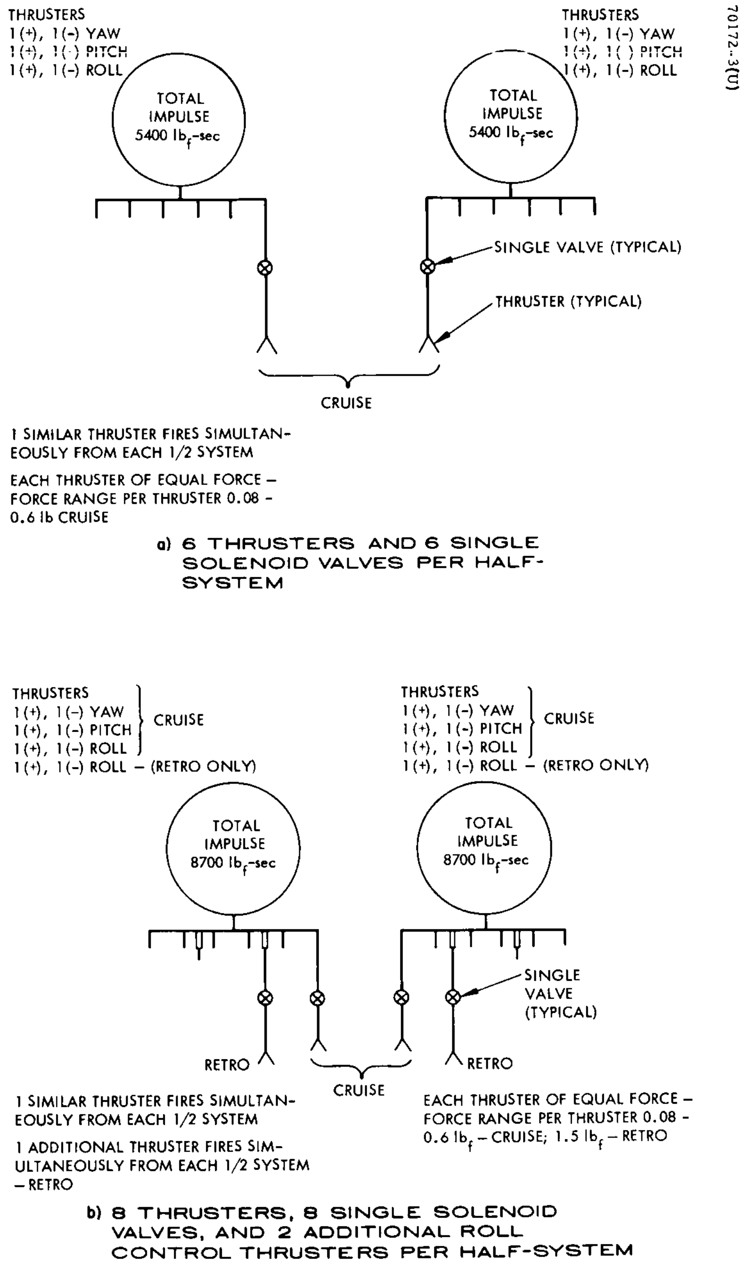


Figure 3. Dual Tank System Configuration

TABLE 2. THRUST SPECIFICATION

Levels	
Cruise	0.04 to 0.08 lb_f
Roll control during retro	0.83 to 1.67 lb_f
Minimum Impulse Bits	
Minimum	16×10^{-4} lb_f-sec
Maximum	120×10^{-4} lb_f-sec
Response times	
Rise time	90 percent of full thrust within 15 milliseconds from command on
Decay time	100 to 10 percent of full thrust within 8 milliseconds from command off

TABLE 3. SUMMARY OF SYSTEM CONFIGURATIONS

Number of Plenums	Impulse per Plenum, lb-sec	Valve Package	Thrust per Nozzle (Number per Plenum)		Series Reference Designation
			Cruise	Retro	
1	3600	Quad	0.08(12)	-	1a
		Dual	0.04(24)	-	1b
	5800	Quad	0.08(12)	1.67(4)	1c
		Dual	0.04(12)	0.83(8)	1d
2	3600	Quad	0.08(6)	-	2a
		Dual	0.04(12)	-	2b
	5800	Quad	0.08(6)	1.67(4)	2c
		Dual	0.04(12)	0.83(8)	2d
2	5400	Single	0.08(6)	-	3a
	8700	Single	0.08(6)	1.67(4)	3b
1	3600	Quad	0.08(6)	-	4a*
		Dual	0.04(12)	-	4b
	5800	Quad	0.08(6)	1.67(2)	4c
		Dual	0.04(12)	0.08(4)	4d

*Series 4 systems are identical to one half of Series 2 systems.

3. GENERAL SYSTEM DESCRIPTIONS

A brief description of each system is given in this section. Diagrams identify only major components. All systems were analyzed in each of the configurations shown in Section 2. The configurations differ in number and type of thrusters and in number of plenums. Where two plenums are required, the configuration shown here was repeated in its entirety. Further details of each system are given in the appendices.

COLD GAS

The cold gas system is shown in Figure 4. The pyrotechnic start valve is optional depending on range safety requirements. The filter may not be necessary or desirable if it is possible to achieve extreme cleanliness. The pressure transducer is shown as typical minimal instrumentation, but items may be added without significant effect on weight or reliability.

Two propellants were examined: nitrogen and Freon 14 (carbon tetrafluoride). The latter is a gas of higher density and compressibility than nitrogen which offers large potential savings in tank weight, more than sufficient to compensate for the decrease in specific impulse.

VAPORIZING LIQUID SYSTEMS

In a vaporizing liquid reaction control propulsion system, the fundamental design question is how to provide the heat of vaporization to the liquid. The heat may be provided by electrical or solar energy directly, or extracted from sensible heat somewhere in the vehicle. We have selected three designs for analysis. The first involves use of an electrically heated vaporizing plenum; the second, a regenerative design, uses an integral tank/boiler which extracts the required heat from the liquid propellant itself; and the third supplements the regenerative design with provision for use of waste heat from the main propulsion engine during the retro maneuver.

Attention has been focused on ammonia and propane propellants. It should be pointed out, however, that a rigorous determination of the best vaporjet propellant for a given mission has never been made. As noted in Appendix D, there are other candidates which may merit attention.

Battery System

The vaporizing liquid system is depicted in Figure 5. In this system, liquid is withdrawn from the low pressure, ambient temperature tank. After being filtered, the liquid is isothermally vaporized.

After vaporization, the saturated vapor is throttled to 50 psia by the pressure regulator and then heated isobarically back to ambient temperature by the superheater. The gas is then passed to the thrusters on demand. The heat required during cruise for vaporization and superheating can undoubtedly be supplied by the ambient waste heat of the spacecraft. The maneuvering and extra roll control, however, require heat to be supplied by electrical heaters. Since the peak power demand is very high, batteries are unavoidable. All power requirements are satisfied by a rechargeable battery, except for the extra roll control case, for which it was more efficient to use an additional special one-shot battery.

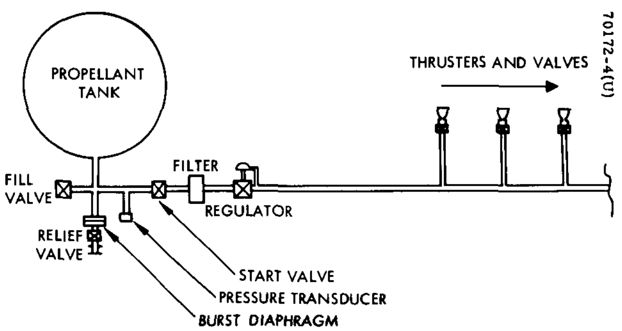


Figure 4. Cold Gas Attitude Control System Baseline Design

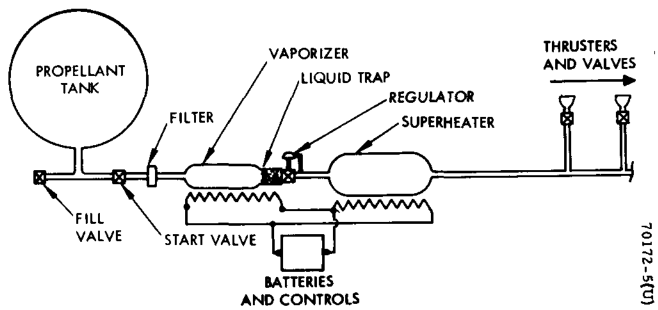


Figure 5. Vaporizing Liquid System With Battery Powered Heaters

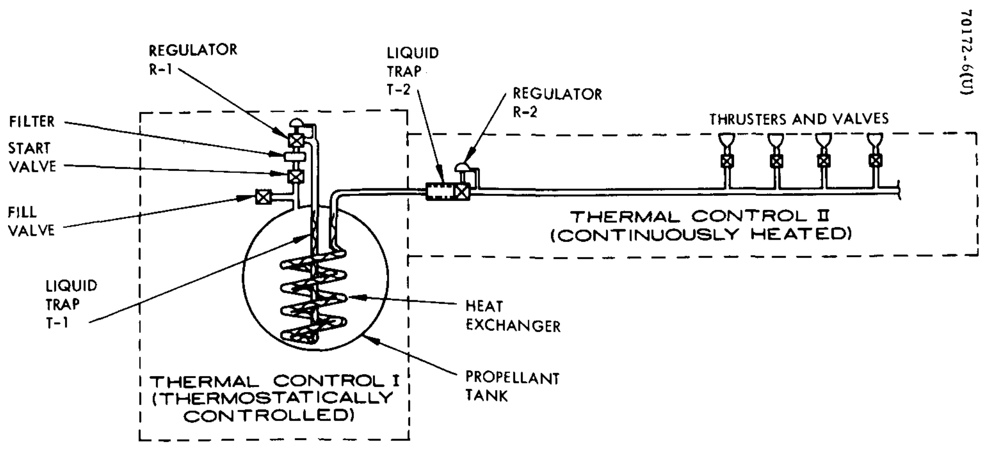


Figure 6. Vaporizing Liquid System With Regenerative Heat Exchanger in Tank

A vaporjet differs from the usual zero-gravity propellant feed system in that the pressurant is not a permanent gas but is the equilibrium vapor whose amount relative to the liquid is quite sensitive to thermal fluctuations. Hence it is not possible to control position of the ullage bubble under zero gravity by bladders or screen devices because vapor pressure will always cause the bubble to appear at the warmest spot in the tank and collapse at all other locations. Because it is important to guarantee the type of feed for efficient vaporizer operation, careful thermal control of the entire tank is needed. In addition, it will probably be necessary to include baffles to reduce sloshing during maneuvers. No detailed investigation was made of these requirements or of vaporizer and liquid trap design because the two systems described in the following paragraphs were more attractive.

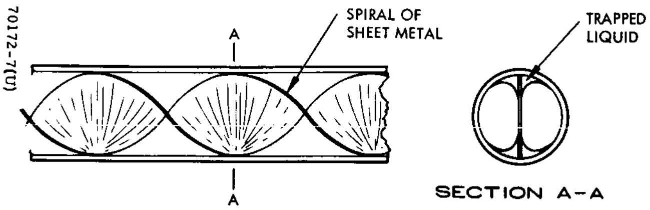
Regenerative Heat Exchanger

Figure 6 shows the method of extracting heat from the propellant tank. Liquid or vapor is withdrawn from the tank and throttled to 70 psia through regulator R-1. The resulting mixture of liquid and vapor passes through the integral heat exchanger and is converted entirely to vapor at approximately tank temperature. The vapor then passes through a trap to remove any remaining unvaporized liquid and is throttled through a second pressure regulator (R-2) to superheated vapor at 50 psia, a pressure appropriate to thruster operation. Temperatures corresponding to the above pressures depend on propellant selection and heat exchanger operation as discussed in Appendix D.

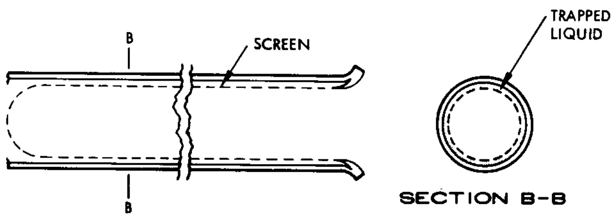
The vapor downstream of regulator R-1 would normally be in the superheated condition. Regulator R-2 functions essentially as a backup to regulator R-1 under conditions in which cool propellant vapor or even liquid propellant may be trapped in the line between regulators R-1 and R-2. This condition might exist, for example, following the retro maneuver, when the temperature of the remaining propellant in the tank will be substantially lower than the temperature during pulse mode operation. Regulator R-2 will maintain the downstream pressure at the desired value while the temperature, and therefore the pressure, of the propellant trapped between R-1 and R-2 increases as the system recovers from the retro maneuver.

Traps for controlling possible liquid intrusions into the feed lines during periods of high flow through the system are indicated by T-1 and T-2. These traps would function by providing a more closely spaced structure than the adjacent tube. Such a structure would act as a "wick" which would hold the liquid by capillary action.

The maximum spacing of the pores of the "wick" would depend upon the accelerations against which the device must hold the liquid. A approximation to the maximum spacing can be obtained by determining the characteristic length which gives a Bond number of unity under the maximum acceleration suffered by the capillary device during the mission. One of the important attributes of a capillary trap for use in a vaporjet system is that the trap liberate no particulate matter that might interfere with the action of valves or regulators. Thus many of the more common materials (such as porous structures of sintered metal or metal felts) were considered to be unsuitable in this application.



a) Trap T-1



b) Trap T-2

Figure 7. Capillary Liquid Traps

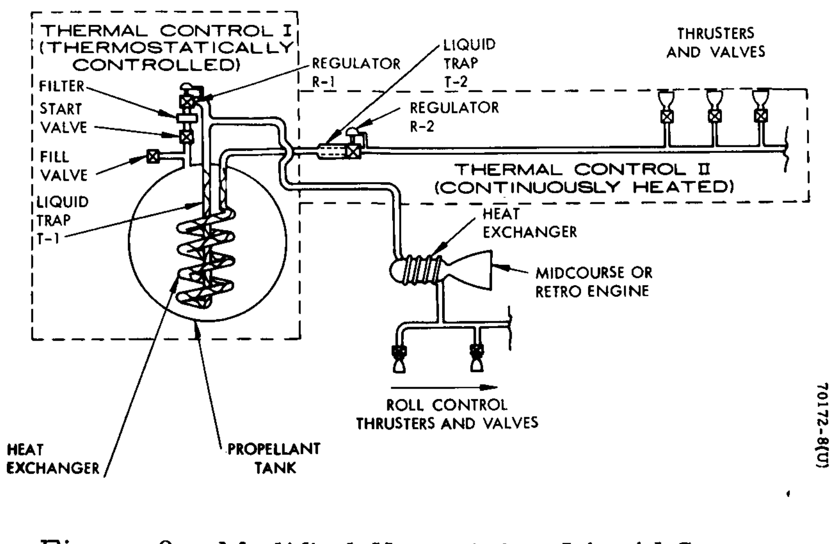


Figure 8. Modified Vaporizing Liquid System With External Heat Exchanger

The types of structures presently envisioned for capillary traps T-1 and T-2 are illustrated in Figure 7. Trap T-1 is fabricated from sheet or screen of the same material as the line in which it is inserted. Liquid in the line will be drawn by capillary action into the corner formed by the edge of the spiral and the tubing wall, as illustrated in Section A-A. The spiral would also provide somewhat improved heat transfer from the vapor to the tubing wall during periods of vapor flow.

Trap T-2 is located immediately upstream of regulator R-2 and is essentially similar to a type of in-line filter inserted at flared joints of AN fittings. Such a filter could probably be employed without modifications; however, the screen mesh would be much finer than that necessary for capillary control of liquid inclusions in the vapor. The trapped liquid would be retained in the annulus between the screen and the tubing, as illustrated in Section B-B. The amount of liquid retained by this method would therefore be limited to the volume of this annulus. Thermal control II (Figure 6) would cause the temperature at trap T-2 to be higher than in other parts of the line between regulators R-1 and R-2 during period of low propellant usage (such as the attitude control mode) and would thus remove by distillation all liquid from trap T-2 (and the neighborhood of the inlet to regulator R-2) during these periods.

Calculations show that tank temperatures of the order of 220° F result in lowest system weight.* Since no source of waste heat is available at that temperature, it is necessary to insulate the tank and provide a low wattage electrical heater and thermostat. Distribution lines downstream of R-2 also must be maintained at a temperature above the dewpoint of the propellant. Thermal control requirements are discussed in Appendix H.

Zero gravity ullage control is not required; the system has been designed to accept either gas or liquid from the tank. Efficiency would be highest if only gas were taken from the tank because no heat exchange would be required. It is quite possible that settling forces during retro would be large enough for such ullage control, but violent boiling of the tank contents might result in some liquid effluent under any conditions, and the suggested system is thought most practical.

External Heat Exchanger System

The external heat exchanger system is shown in Figure 8. This system is a modification of the regenerative heat exchanger system and is applicable only to the extra roll control cases. The modification involves taking the gas or liquid just downstream of the first pressure regulator. The material is transported to a heat exchanger which utilizes the main propulsion system of the vehicle as a heat source. The exiting superheated gas is supplied to the roll control thrusters on demand. All maneuvering and limit cycle operations are performed by the regenerative heat exchanger half of the system described above.

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*See Table D-3, Appendix D.

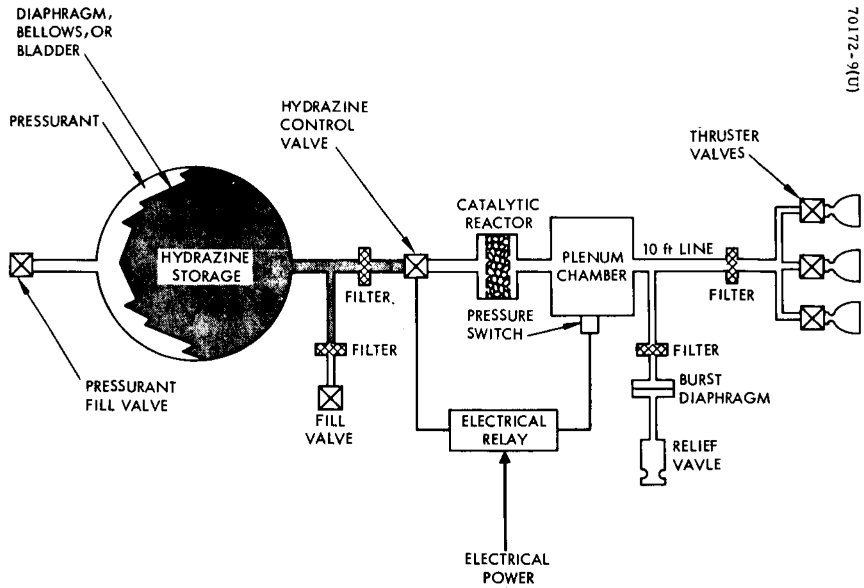


Figure 9. Hydrazine Catalytic Plenum System

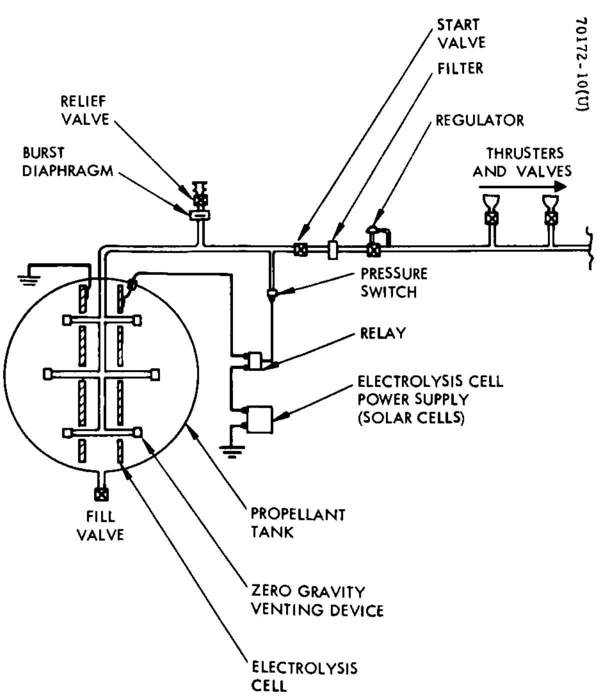


Figure 10. Water or Hydrazine Electrolysis System

There are several advantages in this mode of operation. First, substantial propellant savings result from the fact that extra propellant need not be carried to maintain the tank heat source above the saturation temperature of the gas in the heat exchanger during the high total impulse roll control. Next, this system may prove useful in the thermal control of the main propulsion unit. Finally, additional weight savings can result from the increase in specific impulse due to the higher temperature of the roll control propellant.

Hydrazine Catalytic Plenum

As shown in Figure 9, hydrazine is contained within a tank and segregated from the pressurant gas (nitrogen or helium) by a butyl or EPR rubber bladder. Simple blowdown pressurization is adequate and is preferred over a separate gas bottle and regulator. The bladder was selected for zero gravity ullage control because it is a proved device. Capillary force devices could also be used but offer no particular advantage and are not state of the art.

The catalytic reactor is a monopropellant gas generator of the type developed by JPL and currently available from several companies, including Hamilton Standard Division of United Aircraft, Walter Kidde Company, The Marquardt Corporation, Rocket Research Corporation, and TRW Systems. Control of the generator is accomplished by a pressure switch in the plenum acting on the liquid flow valve through an electrical relay.

A relief valve is considered essential but does not function during normal system operation. A burst diaphragm in series with the relief valve ensures no leakage if valve operation is never required.

Filters are required to remove particles resulting from attrition of the catalyst particles in the gas generator. A filter is shown in the downstream line but could be located in the plenum to enable a greater filter capacity.

ELECTROLYSIS SYSTEM

Either water or hydrazine may be electrolyzed by the system shown in Figure 10. The only differences would be in the materials of construction. (See Appendix E.)

Electrolysis is controlled by a pressure switch and electrical relay which admits current to the cell. Although power demand is low, the cell requires a lower voltage than ordinarily supplied by a spacecraft bus. Power conditioning equipment such as a current or voltage regulator would not be necessary, however, if the cell were connected to its own patch of solar cells.

Operating pressure of the plenum must be considerably higher than acceptable to the thrusters because of the relatively large gas storage requirements. Consequently, a regulator and pyrotechnic start valve are included in the system. The actual operating pressure is 1500 psi to take advantage of Freon 14 prepressurization, shown to be advantageous in the present application.

A bladder is not useful in these systems because gas must be removed from the tank but would be generated on the wrong side of a bladder. The preferred method is a branched manifold terminated by non-wetted porous plugs as shown in Figure 11. So long as any one of the porous plugs is not entirely immersed in liquid, only gas will be passed.

The electrolysis cell consists of concentric wire screen cylinders separated by a wick as shown in Figure 12. Such cells have been tested under Hughes independent research and development and verified to transport and electrolyze liquid. Calculations show the design should be effective, though it has not been experimentally demonstrated under zero gravity conditions.

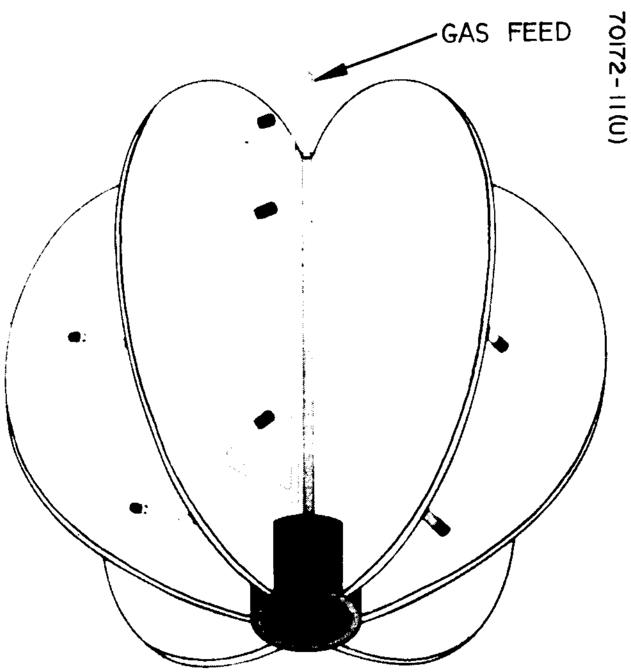


Figure 11. Branched Gas Manifold Baffles and Electrolysis Cell

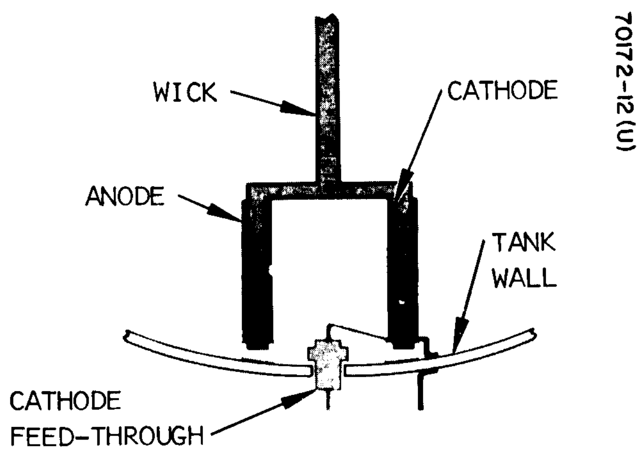


Figure 12. Electrolysis Cell Cross Section

The dual mode hydrazine system is shown in Figure 13. It is composed of all the elements of an electrolysis system with the addition of catalytic monopropellant hydrazine engines and a screen device for liquid feed under zero gravity conditions. In operation, the gas side of the system provides the small accurate impulse bits needed for attitude control, while the liquid engines satisfy all maneuvering requirements. The physical arrangement of the components, shown in Figure 14, is very similar to that of the previous electrolysis systems.

Pressurization of the liquid feed is provided by the generated electrolysis gas which, as in the previously described electrolysis system, is controlled by a pressure switch and electrical relay. In the dual mode system, however, operating pressure is lower because no large gas storage is required and feed pressure for the liquid engines is only about 100 to 300 psia. Suitable liquid engines in the 0.05 to 50 lb_f thrust range are available from the previously listed gas generator manufacturers.

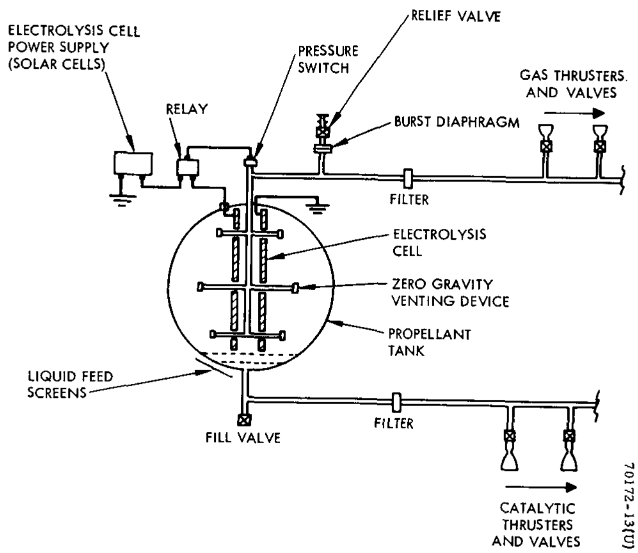


Figure 13. Dual-Mode Hydrazine System

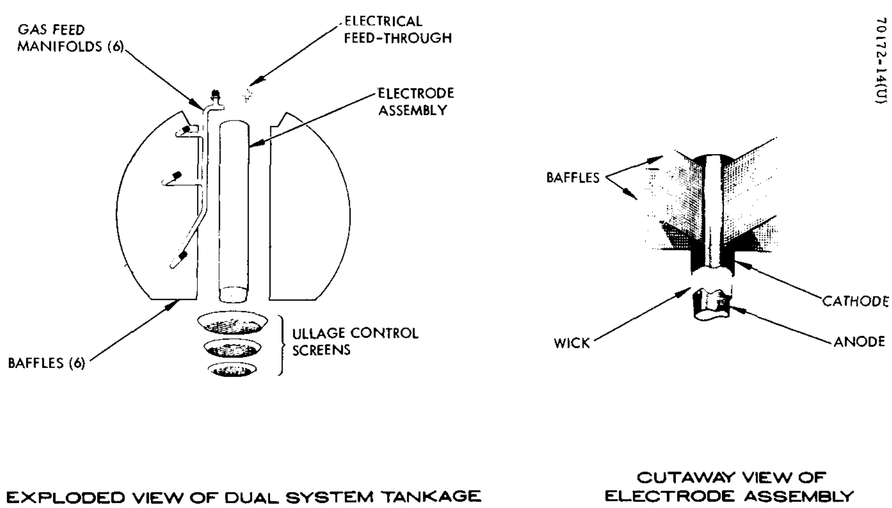


Figure 14. Dual-Mode Hydrazine System Internal Tank Components

4. ANALYTICAL RESULTS

RELIABILITY

From a reliability standpoint, valves were judged the most critical components in any of the systems studied. This conclusion is consistent with the fact that valves have the only mechanical moving parts, except miscellaneous switches of high reliability and a bladder that undergoes only one expulsion cycle. Since identical valving schemes were examined, it is not surprising that overall system reliability is much more a function of configuration than the choice of a particular propulsion method.

The most significant factor in the reliability analysis proved to be the time available for development. The mission under study — a deep space and planetary exploration — is a number of years in the future, and consequently the reliability assessment must be based on projected reliability at the time of execution. For a complex mission, anticipated development time would be 5 years or more; 1972 was selected as the required operational date. This amount of development time is seen to permit considerable growth in attainable reliability. In fact, the results presented in Table 4 show that all of the analyzed systems could attain a very high reliability by 1972, even if the most conservative estimates are adopted. On the other hand, if a system were to be built today, reliability considerations would probably dictate a cold gas system as shown by the graph of reliability versus time, Figure 15. A detailed description of analytical techniques and results is given in Appendix A.

The above reasoning might lead to the conclusion that differences in weight would be more important than reliability in selection of a subsystem for a 1972 mission. It must be realized, however, that the actual selection process would involve optimization of weight and reliability tradeoffs among all subsystems essential to the mission. In such a process, cost and manpower availability might be governing factors. Cost has not, of course, been a factor in the analyses reported here.

Some observations may be offered concerning choice of configuration. One firm conclusion is that the "dual" system which employs two plenums, each containing one and a half times the required propellant and controlled by single valves (series 3), is not desirable from either a weight or a reliability standpoint. It is true that this concept was employed successfully on

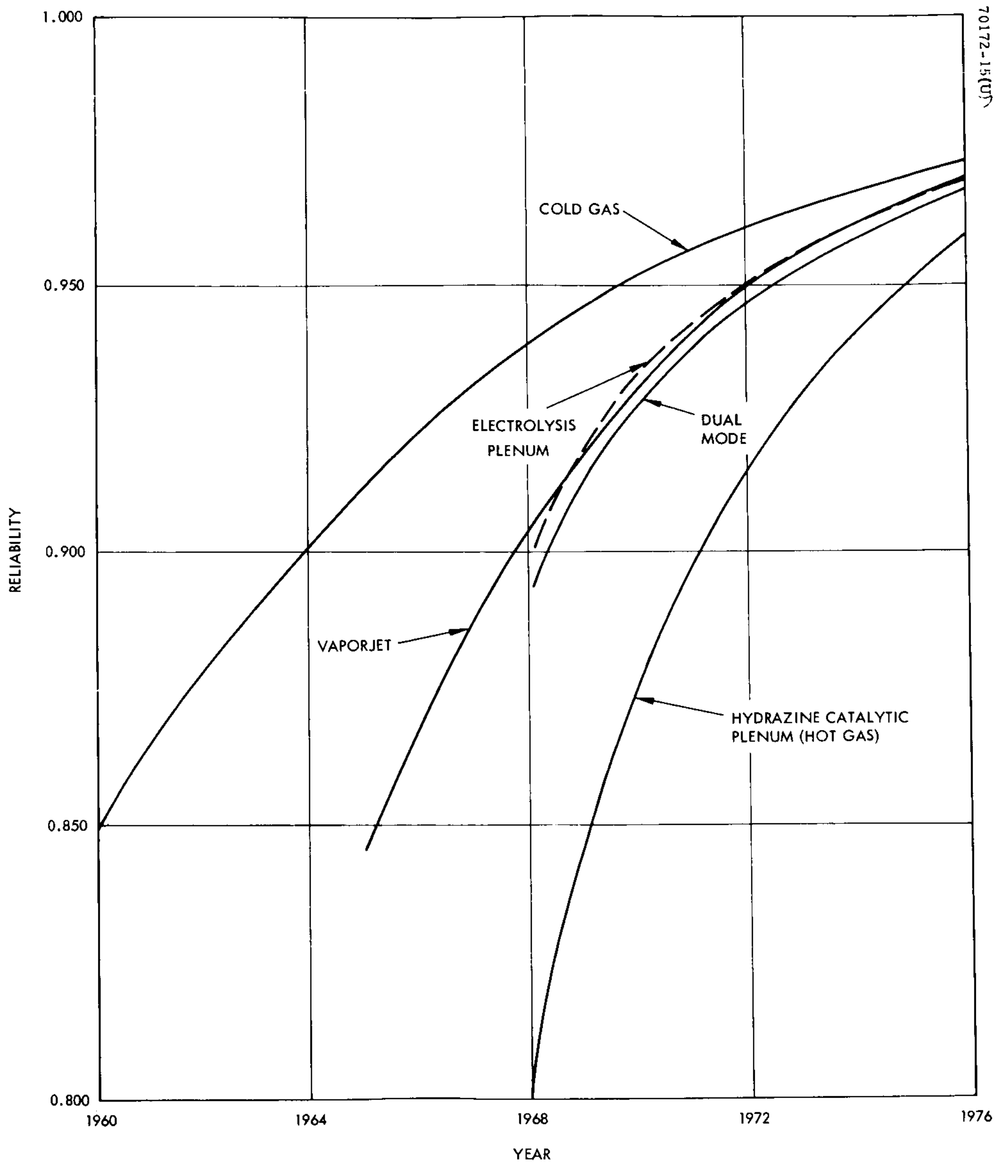


Figure 15. Lower Limit Reliability for Single Plenum, Nonredundant Quad Valves

Designation	Number of Plenums	Total Impulse, lb-sec	Valves	Nitrogen Cold Gas			Freon 14 Cold Gas			Propane Vaporjet, Battery Version		
				Weight, pounds	Reliability Upper	Reliability Lower	Weight, pounds	Reliability Upper	Reliability Lower	Weight, pounds	Reliability Upper	Reliability Lower
1a	1	3,600	Quad	152	0.984	0.958	137	0.984	0.958	105	0.983	0.951
1b	1	3,600	Dual	152	0.984	0.958	138	0.984	0.958	106	0.983	0.951
1c	1	5,800	Quad	247	0.983	0.951	228	0.983	0.951	204	0.981	0.944
1d	1	5,800	Dual	248	0.983	0.951	229	0.983	0.951	205	0.981	0.944
2a	2	7,200	Quad	284	0.998	0.995	254	0.998	0.995	190	0.996	0.991
2b	2	7,200	Dual	285	0.998	0.995	255	0.998	0.995	191	0.996	0.991
2c	2	11,600	Quad	453	0.998	0.994	416	0.990	0.994	366	0.996	0.990
2d	2	11,600	Dual	454	0.998	0.994	416	0.998	0.994	367	0.996	0.990
3a	2	10,800	Single	389	0.982	0.936	343	0.982	0.936	233	0.981	0.931
3b	2	17,400	Single	618	0.971	0.902	562	0.971	0.902	426	0.971	0.896
4a	1	3,600	Quad	142	0.986	0.962	127	0.986	0.962	95	0.983	0.955
4b	1	3,600	Dual	142	0.986	0.962	127	0.986	0.962	95	0.983	0.955
4c	1	5,800	Quad	227	0.984	0.957	208	0.984	0.957	183	0.982	0.949
4d	1	5,800	Dual	227	0.984	0.957	208	0.984	0.957	184	0.982	0.949

TABLE 4. SYSTEM SUMMARY

Ammonia Vaporjet, Battery Version			Ammonia Vaporjet Regenerative Heat Exchanger Version			Ammonia Vaporjet External Heat Exchanger Version			Hydrazine Catalytic Plenum		
Weight, pounds	Reliability		Weight, pounds	Reliability		Weight, pounds	Reliability		Weight, pounds	Reliability	
	Upper	Lower		Upper	Lower		Upper	Lower		Upper	Lower
113	0.983	0.951	84	0.980	0.945	---			65	0.989	0.939
114	0.983	0.951	85	0.980	0.945	---			66	0.989	0.939
262	0.981	0.944	167	0.979	0.939	124	0.979	0.939	102	0.936	0.877
262	0.981	0.944	168	0.979	0.939	125	0.979	0.939	103	0.936	0.877
206	0.996	0.991	148	0.996	0.990	---			109	0.997	0.991
206	0.996	0.991	149	0.996	0.990	---			110	0.997	0.991
482	0.996	0.990	293	0.996	0.989	207	0.996	0.989	158	0.997	0.985
483	0.996	0.990	294	0.996	0.989	208	0.996	0.989	159	0.997	0.985
232	0.981	0.931	184	0.980	0.929	---			125	0.977	0.870
510	0.971	0.896	375	0.970	0.893	243	0.920	0.893	140	0.965	0.725
102	0.983	0.955	74	0.981	0.949	---			56	0.989	0.947
103	0.983	0.955	75	0.981	0.949	---			56	0.989	0.947
201	0.982	0.949	147	0.980	0.943	104	0.980	0.943	89	0.988	0.908
241	0.922	0.949	147	0.980	0.943	104	0.980	0.943	89	0.988	0.908

Dual-Mode Hydrazine System			Hydrazine Electrolysis Plenum			Water Electrolysis Plenum			Combustion Water Rocket		
Weight, pounds	Reliability		Weight, pounds	Reliability		Weight, pounds,	Reliability		Weight, pounds	Reliability	
	Upper	Lower		Upper	Lower		Upper	Lower		Upper	Lower
82	0. 981	0. 934	115	0. 982	0. 939	116	0. 982	0. 939	67	Not determined	
89	0. 981	0. 934	116	0. 982	0. 939	116	0. 982	0. 939	73		
92	0. 978	0. 919	208	0. 980	0. 930	208	0. 980	0. 930	141		
98	0. 978	0. 919	209	0. 980	0. 930	210	0. 980	0. 930	151		
119	0. 997	0. 992	248	0. 998	0. 994	250	0. 998	0. 994	110		
125	0. 997	0. 992	249	0. 998	0. 994	250	0. 998	0. 994	116		
138	0. 997	0. 991	373	0. 998	0. 993	374	0. 998	0. 993	226		
145	0. 997	0. 991	374	0. 998	0. 993	375	0. 998	0. 993	236		
113	0. 960	0. 846	274	0. 981	0. 919	264	0. 981	0. 919	92		
141	0. 935	0. 758	489	0. 970	C. 878	492	0. 970	0. 878	182		
59	0. 983	0. 944	105	0. 984	0. 950	106	0. 984	0. 950	55		
63	0. 983	0. 944	105	0. 984	0. 950	106	0. 984	0. 950	58		
69	0. 981	0. 933	186	0. 982	0. 943	187	0. 982	0. 943	114		
72	0. 981	0. 933	187	0. 982	0. 943	188	0. 982	0. 943	118		

Ranger and Mariner and might still be the method of choice for a small system. In the original Ranger, for instance, it was possible to tank three times the required propellant in a system that weighed only 30.5 pounds, including 4.18 pounds of nitrogen; this amounted to only a 7 pound weight penalty over the simple nonredundant system. Doubling the weight and number of valves probably would have resulted in a greater weight penalty than that incurred by the "dual" half-system approach. For the present mission, valve weight is a much smaller fraction of total weight and the fully redundant half-system approach is found to be excessively heavy.

It is implicit in the above analysis that dual or quad valve groups comprise completely independent valves, totally free from undesirable interactions. While it is felt that sufficient development could, in fact, produce such valves, there is some question whether the assumption is true today. For example, the upstream valve in a dual or quad arrangement could produce contamination by metal attrition from the bearing surfaces which could in turn disable the downstream valve. Additionally, there might be a problem in reproducibility of response for multiple valves acting simultaneously. These questions can be answered only by experiment. To date, the data are insufficient to allow a positive conclusion.

One other interesting conclusion related to quad or dual valves is that redundant valve/thruster packages specified for the Series I configurations would appear to result in an undesirable weight and reliability penalty over a single valve/thruster package for each function. This conclusion follows from the failure rate apportionment, which states that an open (including leak) failure is more likely than stick closed. Two open failure paths appear in Series 1, as opposed to only one path for the simpler system shown as Series 4 in Table 3. Of course, it is realized that some concern about undesirable translation in the flight path may exist for systems that do not provide a pure torque couple, which fact could outweigh the small weight and reliability savings of the simpler system. Replacing nonredundant quad valve/thrusters by a pair of dual valve/thrusters appears to be the most desirable alternative.

The overall conclusion of the reliability study is that all of the systems have high potential reliability according to present standards and the better configurations are very likely to be completely acceptable, even if the most conservative estimates are adopted. Only one case might be considered marginal: the hydrazine catalytic plenum in the additional roll control configurations. This is a consequence of a hot gas valve requirement. The system should, however, still be acceptable in the second configuration.

SYSTEM WEIGHT

Continuing with the task of selecting the best configuration and system, it is seen from Table 4 that the dual mode hydrazine, hydrazine catalytic plenum and ammonia vaporjet systems are sufficiently light in weight that two fully redundant plenums could be employed with a net benefit in both weight and reliability over the best cold gas system. If weight allowance precludes a cold gas or a heavier two plenum system, high reliability is still available in certain single plenum systems. For easier comparison, a weight summary of the three best systems is shown in Table 5.

Either the hydrazine catalytic plenum or the dual mode hydrazine system is lowest in weight, depending on the mission requirements. For the smaller total impulse configurations that do not supply additional roll control, the catalytic plenum is lightest. The larger total impulse configurations, however, favor the dual mode hydrazine system. The reason is that the dual mode system requires an additional set of valves and thrusters, which imposes a significant weight penalty in small systems. As total impulse capability is increased, the better specific impulse obtainable from the catalytic engines of the dual mode system overcomes the weight penalty of the additional hardware. The crossover point occurs somewhere between the two basic configuration requirements.

One other significant factor affects the choice between the two lightest systems. If the hydrazine plenum were called upon to provide additional roll control, calculation indicates that the attitude control thrusters, as well as the special roll control thrusters, would be required to valve hot gas. For maneuvering, however, only cool or moderately warm gas would be distributed, resulting in a much less severe valve problem.

Both factors indicate a preference for the hydrazine catalytic plenum in applications without additional roll control and for the dual mode hydrazine system with added roll control or other large steady state thrust requirements.

The ammonia vaporjet, while not so light in any application as the hydrazine catalytic plenum or the dual mode hydrazine systems, still deserves serious consideration: first, because the ammonia system needs only a straightforward and modest extension of existing technology and, second, because some reduction in the stated weights may probably be achieved. One ground rule for the present study has been that 5 or more years' development time would be available. If the system is required to be operational at a much earlier date or if only a modest funding is anticipated, the ammonia system offers less risk than any system but cold gas. Finally, it is not possible to predict the future of development with confidence. The ammonia (or alternative vaporizing liquid) system is most attractive for a backup development program.

The other systems studied are not recommended for the present mission because of high weight. Cold gas systems in particular suffer a clearly defined weight disadvantage, even for attitude control alone. However, if a given mission could not allow time for systems development, reliability considerations might demand cold gas and it would be important not only to evaluate Freon 14 in more detail but also to investigate other possible cold gas propellants.

The weight and performance of the water and hydrazine electrolysis plenums systems are almost identical. Although they cannot be recommended for the present mission, if attitude control were the only requirement, either could be very attractive.

TABLE 5. SUMMARY OF BEST SYSTEM WEIGHTS

(Total weights in pounds)

System \ Configuration	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Cold Gas — Freon 14	137	138	228	229	254	255	416	416	343	562	127	127	208	208
Vaporjet — ammonia regenerative heat exchanger	84	85	167	168	148	149	293	294	184	375	74	75	147	147
Vaporjet — ammonia external heat exchanger	---	---	124	125	---	---	207	208	---	243	---	---	104	104
Hydrazine catalytic plenum	65	66	102	103	109	110	158	159	125	170	56	56	89	89
Dual mode hydrazine	82	89	92	98	119	125	138	145	113	141	59	63	69	72
Hydrazine electrolysis plenum	115	116	208	209	248	249	373	374	274	489	105	105	186	187
Water electrolysis plenum	116	116	208	210	250	250	374	375	264	492	106	106	187	188
Water electrolysis rocket, combustion	67	73	141	151	110	116	226	236	92	182	55	58	114	118

As a matter of interest, a combustion water electrolysis plenum was calculated. It is seen (Table 4) that the hot water rocket could be lighter in the smaller total impulse configurations than any of the other systems studied. However, additional experimental data confirming solution of the flashback problem would be required in order to make a realistic reliability assessment. Five years of development would probably be more than ample to produce a reliable device.

CONFIGURATION SELECTION

Four basic configurations are considered:

Series 1 - One plenum, dual or quad valves (redundant),
1 x required propellant

Series 2 - Two plenums, dual or quad valves (non-redundant),
2 x required propellant

Series 3 - Two plenums, single valves (non-redundant),
3 x required propellant

Series 4 - One plenum, dual or quad valves (non-redundant),
1 x required propellant

Note that Series 4, while not required by contract, is considered because the reliability analysis indicates superiority over Series 1. The weights are derived from Series 1 by removal of half the quad or dual valve/thruster packages.

Series 3 has been eliminated because of previously described disadvantages in weight and reliability. Series 1 is eliminated unless future experimental data show an unanticipated advantage for redundant quad or dual valving. The choice between Series 2 and Series 4 must be according to acceptable weight because the worst Series 2 system is more reliable than the best Series 4 system.

Two dual valve/thruster assemblies are slightly preferred over a single quad valve/thruster assembly. Reliability is identical according to present knowledge, and the additional weight of the nozzles and/or thrust chambers required for the dual configuration is trivial compared with the total system weight. Since the dual valve configuration may be employed as a pure torque couple, vehicle translation from operation of only one plenum (either by choice or accidental loss of one plenum) can be minimized without recourse to an additional thruster set. Some added benefit can be realized from distributing the leakage torques more uniformly over all axes. Also the dual valve configuration may be made with a smaller thrust chamber volume, and consequent faster pressure transient response, although it is debatable whether improved efficiency, accuracy, or reproducibility of the impulse bit would result. The greatest disadvantage of the dual valve configuration is a change in the impulse bit following a single closed failure. A closed failure in one branch of a quad valve would not alter the total thrust produced but would cut in half the output of a dual valve thruster couple. This characteristic could, however, be turned to advantage if it were possible to turn off half of the dual thrusters after capsule separation, giving the desired reduced impulse bit easily and conveniently. However, reliability would have to be very high in order to depend on such mechanization since a specific half of the thrusters in at least one system would have to be operational.

A decision on the need for additional roll control capability clearly is beyond the scope of the present study since further details of the main propulsion system would need to be known.

SELECTION OF THRUST LEVEL

Selection of retro roll control thruster size is easily made from consideration of the impulse bit, duration of the event, and number of thrusters firing simultaneously. However, selection of the attitude control/maneuvering thruster size is a more complicated question.

It is assumed that maneuvering time requirements are flexible enough to be satisfied by any thruster with the stated force range. It is understood, however, that attitude control is to be accomplished by a derived rate type limit cycle control. In any limit cycle operation it is evident that optimum fuel economy requires a smaller impulse bit after the reduction in spacecraft moment of inertia caused by separation of the landing capsule, which could be accomplished by reducing the thrust level or by shortening the command pulse (or both). In practice such a procedure would likely mean shutting off half the thrusters or changing the electrical time constant in the control network.

Alternatively, an independent set of thrusters could be provided for orbital attitude control. The associated weight penalty can be determined from the tables in the appendices, or approximately by comparison of Series 1 and Series 4 systems in Table 4. Such a weight penalty might be acceptable under certain circumstances, but the problems of changing the derived rate control network must be examined carefully before adding additional thrusters or shutting off a portion of the existing functional thrusters.

Any of the above options would require a command direct from ground control or via the capsule separation scheme. From a reliability standpoint the electrical switching function is equivalent in either mechanization. Note that mechanical switching of throats in each thruster is not considered because not only would a larger number of actions be required but also the mechanical arrangement would be awkward for close coupled valve and nozzle assemblies.

Information to date suggests that the smallest thruster within the specified range would be the best choice, mostly because it permits the greatest pulse width. Specifically:

1) Proper limit cycle operation requires reproducible pulses rather than specific control of rise and decay times. This is most easily accomplished when the pulses are longer than the combined opening and closing time of the valves.

2) Preliminary analysis (see Propellant Performance, below) indicates that boundary layer and other losses are not likely to be an important factor within the specified thrust range and so do not rule out the smaller thrusters.

3) The minimum pulse width required to meet impulse bit requirements is 20 milliseconds for 0.8 lb_f, about the lower limit for reproducibility with commercially available valves. A larger thruster would require a shorter pulse and consequently a valve type which is not available as a qualified component.

4) It is probably not desirable to employ a thruster smaller than 0.04 lb_f, however, because the throat may become too small for easy fabrication. (Further details appear in the nitrogen system analysis.)

PROPELLANT PERFORMANCE

Calculations have been carried out for ammonia and nitrogen using both ideal- and real-gas computer programs. The latter is described in some detail in Appendix I. Both the nitrogen and ammonia results showed very little difference between real and ideal gas models for each of the two cases of equilibrium condensation during expansion and supercooling with no condensation. The effect of condensation was quite slight for nitrogen, but ammonia, having a higher heat of condensation and a higher condensation temperature, showed a substantial increase (of the order of 15 percent) in specific impulse over the case of inhibited condensation. Such a theoretical increase has apparently not been noted previously. The few experimental data available would serve to indicate that a performance enhancement is not realized in practice with ammonia systems, because of the short nozzle residence times involved.

A brief examination of available theory indicated boundary layer losses for both nitrogen and ammonia would be low for the thrust levels involved.

INTERFACE CONSIDERATIONS

It has not been a task of this study to make detailed consideration of interface problems, which would require more specific definition of the other vehicle subsystems and structure. It is clear, however, that the attitude control/maneuvering system may have considerable interaction with other than the usual control interfaces. Electrical power and thermal loads are specific examples.

In some cases it may be desirable or even necessary to integrate the attitude control propulsion system with other subsystems. One example is the ammonia vaporjet, which must be supplied with heat from the main propulsion or retro engines in order to be competitive in the larger total impulse configurations. A second example is the dual mode hydrazine system, which may overlap the midcourse propulsion system in function and components.

It is highly desirable that further study be applied to interface problems, particularly in connection with experimental development programs.

DEVELOPMENT STATUS OF ELECTROLYSIS CELLS FOR ZERO-G APPLICATION

A survey was conducted of the field of zero-g electrolysis cells, because of the need for such a component in the water or hydrazine electrolysis systems. Details of this effort are given in Appendix J. All of the development effort to date, with the exception of a Hughes IR&D program, has been devoted to separated-gas designs. The following conclusions have been drawn from the survey:

1) No suitable electrolysis unit producing separated gases under zero gravity conditions has been found which is available as an off-the-shelf item. The prospects of having such a unit available in the next few years are relatively good because of the important position such a unit occupies in the integrated life support systems currently being developed.

2) At the present time, the chief competitors in the field of separated gas electrolysis cells appear to be Allis-Chalmers, General Electric Missile and Space Company, and TRW Electromechanical Systems. The most promising development work for the mission of interest here is that of the Allis-Chalmers group. This opinion is based upon:

    a) The fact that the relatively well developed General Electric integrated life support unit has experienced serious difficulties in the tests conducted thus far.

    b) The considerable background of Allis-Chalmers with fuel cells of similar design and with other electrolysis cells employing strong KOH as the electrolyte

c) The fact that the Allis-Chalmers design was selected by NASA for the follow-on cell in the integrated life support system program.

3) For the present application, there is no incentive to go to a separated gas design, and the mechanical and design problems associated with the development of an electrolysis cell producing mixed gases appear to be less severe than the problems associated with producing a separated gas system. A mixed gas electrolysis unit would be less complex mechanically than a separated gas unit and would probably be more reliable.

4) Of the separated gas cells surveyed here, the most promising future development is the hydrogen diffusion (Pd-Ag alloy) cathode cell at Battelle, which would produce separated gases without requiring differential pressure regulation, and should offer significant advantages in reliability over other separated gas designs, for long-duration missions.

5) A design life of many years without appreciable deterioration in performance appears to be feasible for the electromechanical cell itself. Limitations on the electrolysis unit life will probably be due to mechanical failures of ancillary equipment. Reliability of the electrolysis units can therefore be estimated from failure rates of the mechanical components alone.

6) No adequate demonstration of the operation of electrolysis cells under zero gravity conditions has been made. A short term zero gravity test carried out by the group at Wright-Patterson Air Force Base was successful.

7) So far as is known to date, hydrazine electrolysis has been investigated only in a Hughes in-house program.

POWER REQUIREMENTS

All of the systems would, of course, require power for solenoid valve operation. These requirements have not been treated in detail because actuation power is highly dependent on response time and specific valve design. The valve design which will result from 5 years of additional development may not be anticipated in sufficient detail to assign firm requirements. However, no unusual problems are predicted for typical spacecraft power systems.

The electrolysis systems are shown to require very low power in the present application. The total watt-hour requirement is rather large, but these systems fortunately store energy in chemical form and so may employ nearly the entire time period of the long mission in absorbing the total requirements. The result is an average power requirement of only a few watts. It is noted, however, that lack of capability for extremely large (several kilowatt) peak demands results in a large storage plenum too heavy for the straight electrolysis systems to be competitive for the present mission. The dual mode hydrazine system has a much lower gas storage requirement and is completely acceptable. The vaporjets require a large amount of thermal energy which may not be supplied by the spacecraft heat ordinarily rejected to space by thermal control processes. Two alternative schemes may be proposed, one of which requires the propellant tank to be maintained at a controlled temperature near 200°F. For an insulated tank, this may be accomplished by only a few watts of electrical energy since there is always a long time for temperature recovery between major steady state thrusting periods. Electrical power on demands for peak thermal loads, however, would amount to several kilowatts, as in the electrolysis systems. The only feasible way to supply these large peak loads is by batteries which are too heavy to be of interest.

HANDLING PROBLEMS AND HAZARDS

The systems considered here offer few new or unusual handling problems or hazards. Table 6 summarizes the anticipated problem areas.

In addition to the problems indicated in the table, all of the systems considered here share two further possible problem areas:

1) Particulate impurities must be carefully controlled to provide assurance that the system valves will operate as expected throughout the mission.

2) If sterilization of the system is required, further experimental work on all systems (with the possible exception of the nitrogen cold gas system) would be required to demonstrate system tolerance to the sterilization procedure.

The pressurized gas systems, the vaporizing liquid systems, and possibly the hydrazine plenum system will contain gases under pressures high enough to be hazardous if a burst should occur in the flight weight tankage. No difficulties due to limitations of the compatibility of materials of construction with the propellant are anticipated for any of the pressurized gases considered. The choice of material for electrical heaters exposed directly to ammonia or other vaporizing liquids would require investigation.

Ammonia and propane will burn in air or other oxidizers; ordinary precautions employed for handling flammable gases should be employed.

While systems employing hydrazine as a propellant present a few additional problems due to the toxicity and flammability of this propellant, a large background of experience with hydrazine as a propellant is available. The systems considered here are expected to be relatively easy to handle in comparison with bipropellant combinations employing hydrazine as a fuel.

TABLE 6. HANDLING PROBLEMS AND HAZARDS FOR VARIOUS ATTITUDE CONTROL SYSTEMS

	N_2	NH_3	N_2H_4 Plenum	N_2H_4 Dual	H_2O Electrolysis
Materials and compatibility problems	Typical high pressure vessel	Typical moderate pressure vessel	Typical for hydrazine	Requires study of electrolysis effects	Requires study of electrolysis effects
Handling problems	High pressure gas handling	Moderate pressure gas handling	Typical for hydrazine	Typical for hydrazine	Typical for water
Hazards	High pressure gas	Pressurized gas; flammable; toxic	Flammable; toxic	Flammable; toxic	Mildly corrosive electrolyte solution
		Possible damage to spacecraft finishes if spilled			

Further experimental work is required to evaluate the effects of electrolytes in the hydrazine which might be employed in electrolysis cells. Three topics are of particular interest:

1) The effects on materials of construction during vehicle preparation, including sterilization if required, and the long period of interplanetary transit.

2) The effects that may occur near the end of the useful life of the attitude control system, when the concentration of the electrolyte becomes very high as the last remaining hydrazine is electrolyzed.

3) The effects, if any, of added electrolytes on the performance of the catalytic thrusters.

When considering the employment of hydrazine in any attitude control system, it should be noted that the specification to which hydrazine is normally produced (MIL-P-26536) is quite tolerant of both particulate and dissolved impurities in the hydrazine. In experimental work with systems that may be susceptible to the effects of particulate impurities (such as the low thrust gas jets considered here) or dissolved impurities (such as electrolysis cell systems), the quality of the hydrazine employed should be evaluated.

The water electrolysis system will probably utilize a dilute alkali or acid as the electrolyte; simple precautions during system servicing should be sufficient to protect both spacecraft and personnel. Materials compatibility problems are not expected, with the possible exception of exposure at temperatures near the boiling point of water (such as the temperatures necessary for vehicle sterilization by heat if this is required). Experimental work would be required to determine the behavior of the electrolysis cell as the supply of water is exhausted and to determine whether any hazard to the spacecraft exists under these conditions.

5. RECOMMENDATIONS

Of the systems examined in this study, the hydrazine catalytic plenum, the dual mode hydrazine, and the vaporizing liquid systems are very attractive in weight and potential reliability. None of the three, however, is adequately developed for immediate application. Specific suggestions for future work are:

Hydrazine Catalytic Plenum

1) Further study of prototype systems.

2) Hot gas valve development if required.

3) Gas generator design for minimization of ammonia and residual hydrazine.

4) Investigation of effects of impurities on catalyst performance and improvement of the specification for hydrazine.

Vaporizing Liquid System

1) Analysis of low gravity heat exchangers and control of liquid ullage for condensable vapors.

2) Study of heat utilization from main or retro engines.

3) Further analysis and experimental study of prototype systems.

4) Search for alternate propellants which could reduce system weight.

Dual Mode Hydrazine System

1) Study of electrolysis process with particular attention to electrolytes and compatibility with materials of construction.

2) Analysis of gas and liquid venting devices under zero gravity.

3) Study of experimental prototype system.

Cold Gas

Although not identified as having weight or reliability advantages compared with the potential capability of the above systems, cold gas is likely to remain of interest for many years because it will continue to deliver the highest reliability at lowest cost for programs which require a very short development time. Despite many years of use it appears that no concerted effort to find the best cold gas propellant has been undertaken. It is suggested that at least a moderate effort be made to find a propellant better than nitrogen, argon, or Freon 14.

General Recommendations

In addition to the recommendations on specific systems, the following suggestions for more general study are made:

1) Study of system interface problems. During the study it was noted that timing of events in the mission sequence could have a very significant effect on required tank ullage, thermal demand, and other factors that ultimately affect system weight. For example, the systems without added roll control were required to provide 500 lb_f-sec just before retro. This impulse bit proved to be a critical design constraint, resulting in penalty to the vaporjet and both electrolysis systems. For those systems, total weight could have been reduced considerably by slight modification of the flight sequence.

   Historically, it frequently has been true that propulsion requirements have been established according to constraints, sometimes arbitrary, exercised by other subsystems; the current trend toward early and detailed tradeoff analyses during the system definition process is tending to improve this situation. The present study might prove to be a case in which propulsion problems could be significantly mitigated by design compromises at an early stage in development.

2) Experimental study of dual and quad valves, with specific attention to interactions between the nominally independent elements. The possibility of quad redundant regulators also merits attention.

3) Updating of experimental data on post-flight acceptance test reliability of valves.

4) Study of effects of contaminant particles of known size, shape, and hardness on both hard and soft seat valves.

5) Measurement of propellant performance over the thrust range of interest, with specific attention to producing and measuring the effects of condensation in the nozzle. Equilibrium calculations show a substantial performance gain might be possible, specifically 15 percent for ammonia, but it is not certain that condensation in the nozzle actually has been induced in any of the few reported experimental programs.

APPENDIX A. RELIABILITY

INTRODUCTION

Reliability is a key factor in the present study. As a preface to the presentation of the assumptions and results, some general discussion of reliability analysis is given.

The major functions of reliability analysis are:

1) Design improvements: Strengthening weakest links and pointing out trouble areas.

2) Comparison: Given System A and System B, determining the more reliable.

3) Prediction: Finding the probability of completing the mission.

The validity of such conclusions concerning a system must finally rest upon the application, number, and reliabilities of the component parts. If the reliabilities of the components were known as a function of time for a given set of conditions (e.g., temperature, electrical stress), the composite reliability of these components could be found through use of standard set theoretical probability. Thus, the problem effectively reduces to finding the reliabilities of the components.

Component failures may be divided into two main categories:

1) Wearout failures. These are failures due to a gradual depletion of material, or fatigue due to a large number of operations. The distribution of failures due to wearout is usually the normal or gaussian distribution, whose mean and variance may be estimated from test of a relatively small sample. Since the number of cycles of operation for a mission is usually known, it is easy to determine whether the probability of wearout is significant. Good reliability design practice should ensure that every effort has been made to reduce this probability to a point at which it is insignificant compared with other factors. Thus the assumption is made in the remainder of this report that resistance to wear-out has been designed in and wearout will be negligible.

2) Random or time independent failures. Often component failures occur that cannot be attributed to gradual depletion of material or fatigue but are due to specific causes and combinations of events whose occurrence cannot be predicted as a function of time.

Different component types have been shown to display different degrees of susceptibilities to such failures, and hence the concept of failure rate has been introduced as a quantitative expression of such tendencies. In mathematical terms, the probability distribution of time to failure of a part with constant failure rate is given by $p(t) = \lambda e^{-\lambda t}$, where the failure rate λ may be statistically estimated by an experiment wherein n parts are placed on life test and failure times noted:

$$\lambda = \frac{\text{number of failures observed}}{\text{total successful part hours on test}}$$

Thus the assumption of the exponential distribution further reduces the problem to assignment of a part failure rate for a given component in a given application.

As components become more reliable, component life tests become less feasible, and truly good failure rate data are usually not available. Further complicating the problem is the fact that the effect of a component failure on a system may depend greatly on the mode of failure (e. g., closed or open for a valve). Thus, it is important not merely to estimate the component failure rate but to apportion that failure rate among the different failure modes.

THE GENERAL SYSTEM MODEL*

The reliability assessment of the configurations in question was conducted by first setting up a general system model.

Redundant System

For a redundant system (dual plenum), failures must be grouped into four categories:

1) Critical failures — Cause mission failure even though a redundant plenum is available — e. g., tank burst.

2) Major open failures — Fail one plenum because of loss of propellant.

*Note: For this discussion a "valve" is the final product, notwithstanding the fact that it may be internally redundant.

3) Major closed failures — Fail one plenum because of inability to operate; however, system remains pressurized.

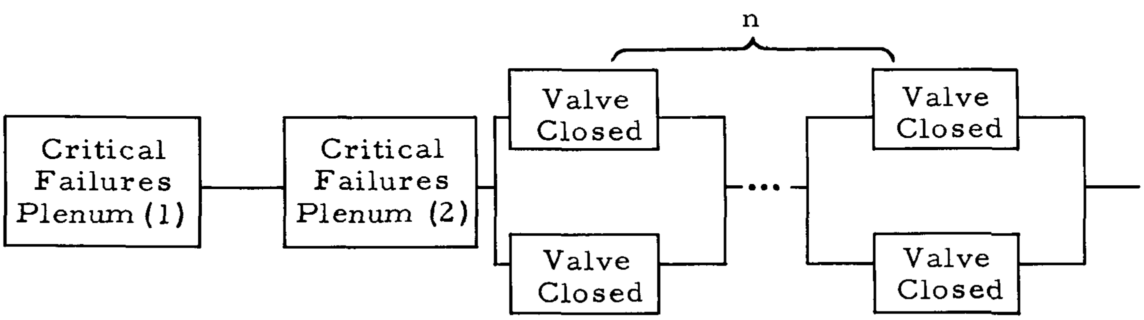
4) Special — not always one of the above. Requires special modeling — e.g., solenoid valves in the closed position.

If R_{MO} is the probability that no major open failure occurs in a given plenum, and R_{MC} is the probability that no major closed failure occurs in a given system, and R_C is the probability that no critical failure occurs, the total dual plenum system reliability may be derived as the sum of the probabilities of the following mutually exclusive events (assuming there are n control valves per plenum):

1) No major failure occurs in either plenum

$$p(1) = R_{MO}^{2} R_{MC}^{2}$$

The reliability block diagram is then:



If the plus yaw jet fails closed in plenum (1), the plus yaw jet in plenum (2) can be used, and so for the other n-1 functions. Hence the reliability equation for the above configuration is:

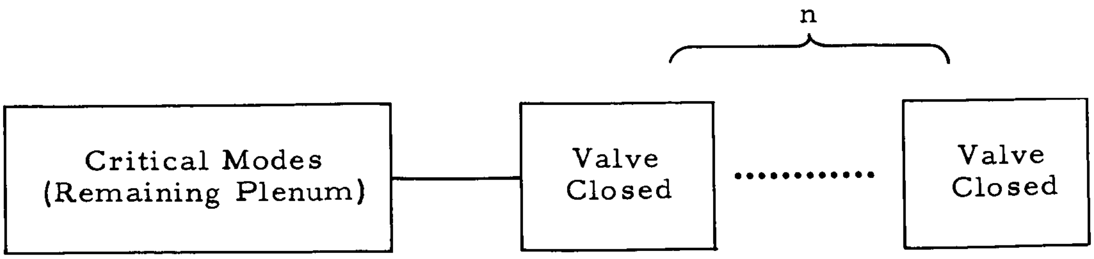
$$R(1) = R_C^{2} \cdot (1 - (1 - R_{VC})^{2})^{n}$$

where R_{VC} is the reliability of a single valve in the closed mode.

2) A major open failure occurs in one plenum, and a critical failure has not occurred previously in that plenum.

$$p(2) = \frac{2\lambda_{MO}}{\lambda_C + \lambda_{M_O} + \lambda_{MC}} R_{MO} R_{MC} (1 - R_{MO} R_{MC} R_C)$$

The reliability block diagram is



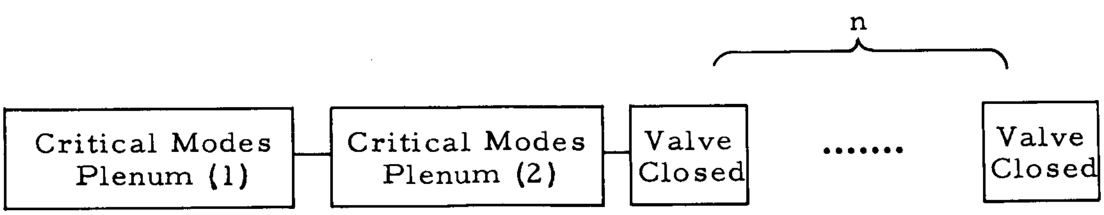
and the equation is*

$$R(2) = R_C \cdot R_{VC}{}^n$$

3) A major closed failure occurs in one plenum

$$p(3) = \frac{2\lambda\ M_C}{\lambda_{MO} + \lambda M_C} R_{MO} R_{MC} (1 - R_{MO} R_{MC})$$

The reliability block diagram is



$$R(3) = R_C{}^2 \cdot R_{VC}{}^n$$

4) Major failures occur in both plenums

$$p(4) = (1 - R_{MO} R_{MC})^2$$

$$R(4) = 0$$

*This formula accounts for the fact that failure in the closed position after failure in the open position has no effect, and vice versa.

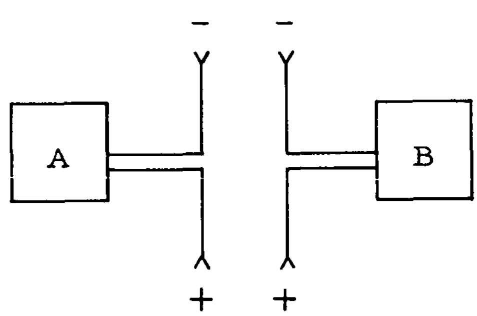
The total reliability is then

$$
\begin{aligned}
& p(1)R(1) \\
+ & p(2)R(2) \\
+ & p(3)R(3) \\
+ & p(4)R(4) = R_{MO}^2 R_{MC}^2 R_C^2 \left(1 - (1 - R_{VC})^2\right)^n \\
& + \frac{2\lambda M_O}{\lambda_C + \lambda_{MO} + \lambda_{MC}} R_{MO} R_{MC} (1 - R_{MO} R_{MC} R_C) R_C R_{VC}^n \\
& + \frac{2\lambda MC}{\lambda_{MO} + \lambda_{MC}} R_{MO} R_{MC} (1 - R_{MO} R_{MC}) R_C^2 R_{VC}^n
\end{aligned}
$$

The above must be modified for certain special situations. An example is the case in which a valve fails in the full open position. If this happens at an early stage in the mission, the failure could be critical. This phenomenon will now be considered.

Derivation of Critical Time, T_c, for Dual Plenum System

Consider a system composed of two subsystems, each consisting of one tank, one "positive" valve, and one "negative" valve:



Suppose:

1) Each subsystem contains a total impulse of I_o — exactly that required to complete the mission.

2) Both positive and/or both negative valves are commanded by the same signal.

3) Fuel consumption is balanced between the two tanks.

If a full open failure occurs at a time when total impulse I has been used, the following will take place (assume for definiteness that the failure occurred in thruster A^+):

1) Thrusters A^- and B^- will be actuated by limit cycle control, each at half the effective bit rate of thruster A^+. Total impulses are equal and opposite.

2) $(I_o - I/2)$ will be expended from tank A (all the remaining fuel).

3) $1/3(I_o - I/2)$ will be used from tank B to counter this force and thus $2/3(I_o - I/2)$ is remaining to complete the mission.

4) $(I_o - I)$ is required to complete the mission.

The defining relation is:

$$I_o - I \leq 2/3(I_o - I/2) \quad \text{for success} \quad ,$$

or

$$I \leq I_o/2$$

Let

$$I_c \equiv I_o/2$$

Then, if full open failure occurs before I_c is used, mission failure results. In this way, T_c is defined as that time when I_c impulse has been used from the mission profile. In the present instance, $T_c = 133.5$ days (3204 hours).

It must be noted that the assumption of equal fuel consumption implies no failures in a closed position. The effects of closed failures on the computation would be as follows (assuming A^+ failed full open):

1) If A^- fails, all of fuel in B would be used, resulting in mission failure.

2) If B^+ fails, all (+) capability would be lost.

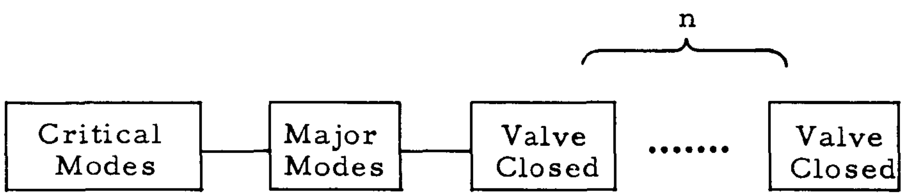
3) If B^- fails, all (-) capability would be lost, resulting in mission failure.

Cases 2 and 3, however, are already accounted for in the model of "valve group" reliability.

Case 1 requires a dual failure. The probability that this does not occur is $(1 - (1 - R_{VF}(M)(1 - R_{VC}(M))$. This dual failure is considered in the reliability formulas later in this appendix (see The General Reliability Formula).

The Single System

The reliability block diagram of the single system is simply



and the reliability equation is

$$R_C \cdot R_{MC} \cdot R_{MO} \cdot R_{VC}^{\,n}$$

Effects of Component Failure Rate Ranges on System Reliability

Although it is desirable to estimate the failure rate as accurately as possible, the result often has less precision than desired and it is important to assess the effect of a failure rate assumption. If uncertainty in the component reliability values exists, there are several approaches to the reliability evaluation, among which are:

1) The Monte Carlo Analysis — A computer is programmed to "choose" samples of component failure rates from probability distributions placed upon these failure rates. A system reliability number is then computed for each "set" of failure rates chosen. This process is repeated several hundred times, the result being a probability distribution on the system reliability.

2) Extremes Analysis — Lower and upper reliability limits are determined for each component. The lower limit on system reliability is then computed using lower limits for component reliabilities, and the upper limit on system reliability is computed using upper limits on component reliabilities.

Both the above methods will be sufficient to yield the following types of information:

1) Comparison between different systems at various stages of development. A new system may have greater achievable reliability while an older system may, in fact, have a better chance of providing acceptable reliability given the development time available.

2) Determination of the optimum configuration. The number of weak or unknown components may be minimized by proper application of redundancy.

The method to be used here will be the simpler extremes analysis. The result will be a useful range of system reliabilities that will point out the sensitivity of subsystem reliability value to the unit reliability assumptions. The procedure is most valuable for comparison of systems, because some systems are less sensitive to changes in unit failure rates than others, and the method adequately reflects the fact that some uncertainty does exist.

RELIABILITIES OF QUAD AND DUAL VALVES

Quad and dual valve configurations may provide increased redundancy but also introduce additional failure modes through increased complexity. The method of analysis of these components follows.

<u>Dual Valves</u> (assuming independence of failures)

<u>Closed Mode</u>

For failure in the closed mode, only one of the two need fail. Thus, if R_{VC} is the reliability of a single valve, reliability in the closed mode is:

$$_{D}R_{VC} = R_{VC}^{2}$$

<u>Open Mode</u> (i. e. , leak or full open)

For failure in the open position, both single valves must fail. Thus, if R_{VO} is the reliability of a single valve in the open mode, the reliability of a dual valve in the open mode is:

$$_{D}R_{VO} = 1 - (1 - R_{VO})^{2}$$

Full Open Mode

For failure in the full open mode, both single valves must fail in the full open position. Thus, if R_{VF} is the reliability of a single valve in the full open mode, the reliability of a dual valve in the full open mode is:

$$_D R_{VF} = (1 - (1 - R_{VF})^2)$$

Leak Mode

A failure in the leak mode is defined as any open failure which is not a full open failure. Thus, if R_{VF} and R_{VO} are as defined above, the reliability of a dual valve in the leak mode is:

$$_D R_{VL} = 1 - (1 - R_{VO})^2 + (1 - R_{VF})^2$$

Quad Valves

If the reliability of the thruster is 1.0000, since only a nozzle is required, a quad valve package would have a reliability equivalent to that of two dual valves in parallel, providing redundancy for the closed mode but operating in series for the open mode.

Then:

$$_Q R_{VC} = 1 - (1 - R_{VC}{}^2)^2$$

$$_Q R_{VO} = (1 - (1 - R_{VO})^2)^2$$

$$_Q R_{VF} = (1 - (1 - R_{VF})^2)^2$$

$$_Q R_{VL} = 1 - (1 - R_{VO})^2 - \left[(1 - R_{VF})^2\right]^2$$

Correction for Non-Independence of Failures

The preceding analysis assumed independence of failures. This assumption seems reasonable, given proper selection of materials and filtration. In addition, representatives of the Valcor Company reported in a

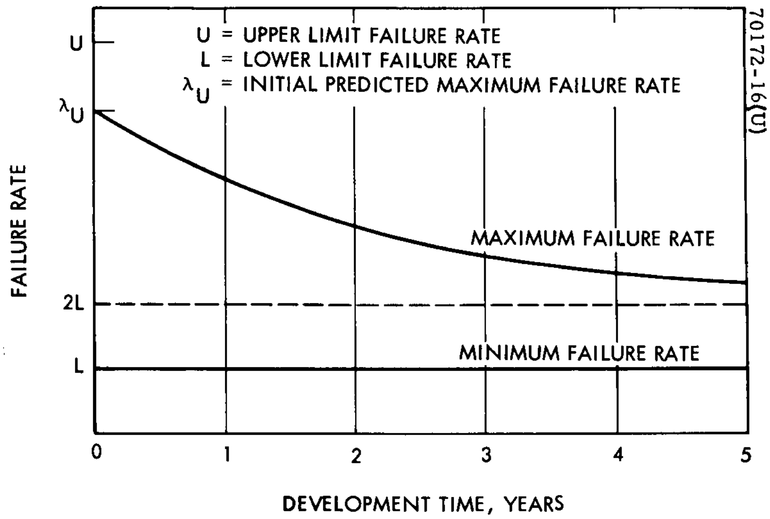


Figure A-1. Effect of Development Time on Failure Rate presentation to Hughes that in their experience with over 1200 quad valves there was no instance of non-independent failure. Nevertheless, for the sake of completeness and as recognition of the possibility that further investigation may reveal the existence of non-independent failure modes, a reliability formula for such a case is given below.

It may be assumed that the non-independence refers only to the two valves in series with each other and that the parallel branches are still independent. Thus, the reliability formulas for ${}_DR_{VC}$ and ${}_QR_{VC}$, i.e., the closed modes, need not be altered.

If $1 - R_{VN}$ is the probability of a non-independent failure in the open mode,

then:

$$ {}_DR_{VO} = (1 - (1 - R_{VO})^2)R_{VN}^{\ 2} $$

$$ {}_QR_{VO} = (1 - (1 - R_{VO})^2)R_{VN}^{\ 4} $$

It is believed that this is the only change that need be considered. It should be noticed that R_{VN} is a critical parameter as it appears in the above expression to the fourth power.

DERIVATION OF COMPONENT FAILURE RATES

To compare the relative reliabilities of a series of systems that are to be launched some years in the future and are composed of a number of components yet to be fully developed, it is necessary to develop a consistent methodology for assignment of component failure rates as a function of time. In this analysis, component reliabilities were predicted from 1) generic part failure rate, 2) technical problems involved in the specific application, and 3) development time available. These quantities are to be related as shown in Figure A-1, in which L is the lowest published failure rate of the generic type, U is the greatest published failure rate of the generic type, and λ_U is a function of the technical problems associated with the application. The value of such a treatment is that it allows comparison, in a consistent and quantitative manner, of systems differing in constitution and degree of development.

The Method

Generic Part Failure Rate Range

The random catastrophic failure rate of a given component in a specific application can be said to be known only after actual operational data have been gathered on identical components in identical environments.

Even aside from statistical considerations, as soon as any of the characteristics of either the device or application are changed, some uncertainty must exist as to the actual resultant failure rate, and thus the assignment of a single failure rate to a new component in a given application would be inadequate without some statement of the uncertainty involved. In the present analysis, the minimum and maximum generic failure rates for a component were defined as follows:

1) Minimum — The minimum failure rate will be the inherent failure rate of the component given that all design problems have been solved. The minimum failure rate will thus be the same for all parts of a particular generic type regardless of the application. The underlying assumption is that, given sufficient effort, any problems connected with the application can be "designed out." The system reliability computed from the minimum failure rates will thus represent the inherent or ultimate possible reliability of the system.

2) Maximum — The maximum failure rate for a generic type represents the worst that could be expected of a component that has had sufficient development to be considered approved for flight and therefore is an acceptable device for the application.

   The maximum generic part failure rate for the type represents the worst that would be expected under the least favorable application considered. This maximum will be later adjusted to include characteristics of the particular application.

   A literature search on reliability data of attitude control systems indicate that the Avco reliability failure rate handbook* at this time contains the most reasonable and complete range of generic failure rates available. The lower limits and upper limits published in that source were used as the minima and maxima for this study.

Quantification of Level of Technical Difficulty

The technical problems involved in producing an acceptable device were quantified through engineering judgment by means of the failure modes and effects analyses presented later in this appendix (see Failure Modes and Effects Analyses).

Each problem encountered for the component in question will be assigned a number 1 to 5 depending on the degree of technical challenge presented (5 for greatest challenge). The total will then be computed for each component and will be designated by Q_i. The values of Q_i will be

*D. R. Earles and M. F. Eddins, Avco Reliability Engineering Data Series, Failure Rates, April 1962.

compared for components of the same type, but in the various systems considered, the largest Q_i thus found will be designated Q_{MAX} and a relative index of difficulty may be assigned to the i(th) component of the type as Q_i/Q_{MAX}. In this manner, the solenoid valves in a nitrogen system, for example, may be compared with the solenoid valves in a hydrazine system as to degree of difficulty of producing a reliable product.

Q_i/Q_{MAX} will then be used to determine a relative value of maximum failure rate of the component in its application (with the understanding that only preflight development has taken place) according to the following model:

Let L denote the lower limit generic failure rate for the type and U the upper limit. Then the following constraints will prevail:

1) No component will have a "maximum" failure rate less than 2L; i. e., there is always a variability of at least a factor of two within inherent failure rates of components in a given application.

2) No component will have a maximum failure rate greater than U.

3) The increase over and above 2L will be directly proportional to Q_i, the relative index of technical difficulty.

The above assume that the total failure rate of the component is equal to the inherent failure rate plus a contribution from each possible problem area.

The component failure rate maximum for a given part in its application will then be computed according to

$$\lambda_U = 2L + \frac{Q_i}{Q_{MAX}} (U - 2L)$$

$$= 2L(1 - Q_i/Q_{MAX}) + Q_i/Q_{MAX}U \qquad (1)$$

where L is the lower limit on failure rate for the generic type, U is the upper limit on failure rate for the generic type, and Q_i is the sum of the points awarded for technical difficulty of this application by the failure modes and effects analysis. Q_{MAX} is the largest value of Q_i found for the components of this type among the systems considered.

The component failure rate minimum for a given part in its application will be defined as

$$\lambda_L = L \qquad (2)$$

Quantification of Development Effort

Reliability growth in terms of failure rate decrease may reasonably be assumed to follow an exponential expression of the form

$$\lambda(T) = \lambda_o + \lambda_1 e^{-KT} \tag{3}$$

where T indicates the level of development effort expended.

Since it has been postulated that λ_L, corresponding to the maximum inherent reliability, should not be a function of development effort, the decrease of maximum failure rate with development will be considered here.

The failure maximum for a component in a given application with development time T will be given by

$$\lambda_U(T) = 2L + (\lambda_U - 2L)e^{-KT} \tag{4}$$

where L is the lower limit generic failure rate, λ_U is given by Equation 1, and T is the time in years between the date at which the first flight hardware would be available and 1 January 1973, the date for which the failure rates are predicted.

The parameter K was chosen by assuming that 80 percent of all technical problems would be solved within 5 years from the first flight hardware; i. e., $e^{-5K} = 0.2$, or $K = 0.321$. Selection of this value is in accord with experience; e. g., the extensive reliability data for Falcon missiles agree well with this treatment. It is seen from Equation 4 that

$$\lambda_U(0) = U$$

$$\lambda_U(\infty) = 2L$$

FAILURE MODES AND EFFECTS ANALYSES

Failure modes and effects analyses are presented in Tables A-1 through A-5 for the electrolysis, nitrogen, hydrazine plenum, ammonia, and liquid hydrazine systems. The two electrolysis systems are treated identically. As the dual hydrazine system is part electrolysis and part liquid, the liquid portion has a separate failure mode analysis.

TABLE A-1. FAILURE MODES AND EFFECTS ANALYSIS – NITROGEN SYSTEM

Item	Failure Mode	Probable Cause	Probable Effect	Relative Likelihood A – E	Degree of Technical Difficulty 1 – 5	Development by 1972
1) Tank	Burst	Failure of weld and overpressure	Catastrophic to mission	D	2	12 years +
	Leak	Crack in joint	Subsystem failure	C	2	
2) Manifold	Leak Burst	Excessive vibration or attack of material and welds	Loss of propellant Catastrophic	C D	2 2	12 years +
3) Fill valve	Leakage	Seal deterioration (can be prevented by capping)	Loss of propellant. Subsystem failure	D	1	
4) Squib valve	Failure to open	Failure to fire, electrical failure, or jamming	Subsystem failure	D	2	12 years +
5) Filter	Passage of contaminant	Large pressure differential and breakthrough, discontinuity in element	Possible sticking of control valve – minor to major	D	1	12 years +
	Clogging	Excessive contaminant in propellant	Failure to feed propellant	D	1	
6) Control valve	Failure to open	Electrical failure Armature sticking Adhesion and corrosion	Loss of thruster	B	1 1 2	12 years +
	Failure to close	Spring failure or armature sticking	Engine may not stop firing	D	1	
	Leak (excessive)	Dirt on seat Seal leak Attack of elastomers	Loss of propellant and subsystem failure	A	1 4 1	
7) Nozzles	Skirt damage	Micrometeorite impact	Degradation only	D	N.A.	N.A.
8) Regulator See Control valve						
9) Relief valve						

TABLE A-2. FAILURE MODES AND EFFECTS ANALYSIS — HYDRAZINE PLENUM SYSTEM

Item	Failure Mode	Probable Cause	Probable Effect	Relative Likelihood A — E	Degree of Technical Difficulty 1 — 5	Development by 1972
1) Tank	Burst	Overpressure	Loss of propellant	D	1	4 years +
		Material deterioration	Mission failure		3	
	Leak	Crack in material or joint	Loss of propellant depends on rate	C	1	
2) Bladder	Leak	Porosity or crack in base material	Mixing of propellant and pressurant, may only degrade	D	2	11 years
3) N_2H_4 fill valve	Leak	Seal deterioration	Loss of propellant through leakage	E (will be capped)	1	12 years +
4) Filters						
N_2H_4	Passage of contaminant	Excessive contamination or discontinuity in element	Sticking of N_2H_4 flow valve and loss of control	D D	1 1	11 years
	Clogging	Excessive contamination	Failure to feed fuel — loss of subsystem	E	1	
Hot gas filter	Passage of contaminant	Excessive contamination or discontinuity in element	Possible sticking of control valves if N_2H_4 filter fails in same mode	C D	5 1	4 years
	Clogging	Excessive contamination	Failure to feed control valves	C	5	
*5) Control valves	Failure to open	Electrical failure	Loss of thruster	C	1	4 years
		Armature sticking			1	
		Adhesion and corrosion			7 (12)	
	Failure to close	Spring failure	Engine will not stop operating	B	2	
		Armature sticking			1	
	Leak	Frozen valve (high temperature)		D	0 (5)	
		Dirt on seal			4 (10)	
		Seal leak			4 (10)	
		Attack of elastomers			3 (0)	
6) Nozzle	Skirt damage	Micrometeorite impact	Degradation only	D	1	12 years

*Figures in parentheses are for systems with roll control.

Table A-2 (continued)

Item	Failure Mode	Probable Cause	Probable Effect	Relative Likelihood A – E	Degree of Technical Difficulty 1 – 5	Development by 1972
7) N_2H_4 valve	Failure to open	Electrical failure	No gas generation	C	1	11 years
		Adhesion, freezing, corrosion			5	
	Failure to close	Spring failure or armature sticking	Gas dumped overboard through pressure relief valve	D	1	
					1	
	Leak	Dirt on seal	Loss of propellant	C	1	
		Seal leak			1	
		Attack of elastomers			3	
8) Plenum chamber and catalyst bed	Burst	Thermal and pressure cycling	Loss of propellant; could be catastrophic	E	1	4 years
	Leak and catalyst attrition, loss of catalyst and powdering of catalyst	Cycling	Loss of propellant	C	1	
		Overheating	Performance degradation	C	3	
		Failure of retaining screen	Performance degradation	E	0	
		Improper packing, loss of catalyst	Local accumulation of powder	E	0	
9) Pressure switch	Failure to open N_2H_4 valve	Electrical failure	No gas generation	C	1	12 years
	Failure to close N_2H_4 valve	Electrical failure	Loss of fuel out of relief valve	C	1	
*10) Pressure relief valve	Failure to open	Armature sticking	Overpressurization	D	1	4 years
	Failure to close	Spring failure or armature sticking	Gas dumped overboard	D	1	
			Loss of propellant	C	1	
	Leak	Seal leak	Loss of propellant		1	
11) Manifolding	Break or leak	Vibration or shock	Catastrophic loss of propellant	D	2	12 years
		Attack of material or welds	Catastrophic loss of propellant	C	3	

*Relief valve used only in case of pressure switch failure and thus has small effect on overall reliability.

TABLE A-3. FAILURE MODES AND EFFECTS ANALYSIS – AMMONIA SYSTEM

Item	Failure Mode	Probable Cause	Probable Effect	Relative Likelihood A – E	Degree of Technical Difficulty 1 – 5	Development by 1972
1) Tank	Burst	Overpressure	Loss of propellant may be catastrophic	D	1	8 years
	Leak	Crack in base material	Loss of propellant	C	1	
2) NH_3 fill valve	Leak	Seal deterioration	Loss of propellant	E (would be capped)	1	12 years
3) Filter	Passage of contaminant	Excessive contaminant	Possible sticking of control valve	D	1	11 years
		Discontinuity in element			1	
	Clogging	Excessive contaminant	Failure to feed fuel	D	1	
4) Control valve	Failure to open	Adhesion and freezing			2	8 years
		Electrical failure	Loss of thruster		1	
		Armature sticking			1	
		Corrosion			2	
		Attack of elastomers			3	
	Failure to close	Spring failure	Engine will not stop firing	C	1	
		Armature sticking			1	
	Leak	Dirt on seal	Loss of propellant	A	1	
		Seal leak			1	
5) Combustion chamber	Burst	Weld failure or leak of fittings	Loss of propellant	D	1	8 years
	Leak			C	2	
	Heater failure	Electrical failure	No gas generated	B	1	12 years
6) Nozzles	Plugging in throat	Contamination	Loss of thruster	E (filter fitted)	1	12 years
7) Pressure switch	Refuse to open	Electrical failure	No gas generation	D	1	12 years
	Refuse to close	Electrical failure	Fuel dumped out relief valve	D	1	
8) Manifolding	Burst	Vibration or shock	Catastrophic	D	1	12 years
	Leak	Attack of material or welds	Loss of propellant	C	2	
9) Regulator See Control valve						

TABLE A-4. FAILURE MODES AND EFFECTS ANALYSIS — [illegible] OR WATER ELECTROLYSIS SYSTEMS

Item	Failure Mode	Probable Cause	Probable Effect	Relative Likelihood A – E	Degree of Technical Difficulty 1 – 5	Development by 1972
1) Tank	Burst	Overpressure	Loss of propellant	D	3	8 years
	Leak	Crack in base material Deterioration (KOH)		C	3	
2) Electrolysis cell	Failure to electrolyze	Failure of leads	No gas generated	C	1	4 years
		Failure of power supply			2	
		Failure of pressure sensing			2	
	Failure to stop electrolysis	Failure of pressure	Gas dumped out	C	2	
		Sensing of power supply			2	
3) Relief valve	Leakage	Spring breakage Seal deterioration	Loss of propellant	C	4	4 years
	Failure to open	Poppet wedged in seat	Overpressurization	C	2	
	Failure to close	Spring breakage	Loss of propellant	D	1	
	NOTE: Relief valve needed only if improper pressure or sensing.					
4) Pressure switch	See 2					
5) Filter	Passage of contaminant	Discontinuity in filter	Sticking of control valve	D	1	
	Clogging	Excessive contaminant	Failure to feed thrusters	D	2	
6) Control valves	Failure to open	Adhesion to corrosion and freezing		C	3	6 years
		Electrical failure	Loss of thruster		1	
		Armature sticking			1	
	Failure to close	Spring failure	Engine does not stop firing	D	1	
		Armature sticking			1	
	Leak	Dirt on seat	Loss of propellant	A	1	
		Seal leak			5	
		Attack of elastomers			1	
7) Manifolding	Burst	Vibration or shock	Catastrophic	D	1	12 years
	Leak	Attack of material or welds	Loss of propellant	C	2	
8) Regulator	Stick open	Contamination spring failure	Loss of propellant	C	1	
	Stick closed	Diaphragm	No gas generation	B	2	
	Leak	Dirt or contamination	Loss of propellant	C	1	

TABLE A-5. FAILURE MODES AND EFFECTS ANALYSIS – LIQUID HYDRAZINE SYSTEM FOR DUAL MODE SYSTEM

(Only components not included in electrolysis system)

Item	Failure Mode	Probable Cause	Probable Effect	Relative Likelihood A – E	Degree of Technical Difficulty 1 – 5	Development by 1972
1) Combustion chamber and catalyst	Burst	Overpressurization	Loss of propellant (catastrophic)	D	1	6 years
	Leak	Crack in material or welds	Loss of propellant	C	1	
	Catalyst attrition	Performance degradation		C	3	
2) Control valves	Fail to open	Adhesion Freezing shut and corrosion	Loss of thruster	C	1 5	11 years
	Fail to close	Armature sticking Spring failure	Engine will not stop firing	D	1 1	
	Leak	Armature sticking Dirt on seal Seal leak Attack of elastomers	Loss of propellant	B	1 1 1 3	
3) Fill valve	Leak	Seal leak	Loss of propellant	E (will be capped)	1	12 years
4) Filter	Passage of contaminant	Excessive contaminant	Possible sticking of control valve	C	1	11 years
	Clogging	Discontinuity Excessive contaminant	Loss of control capability	C	1 1	
5) Manifolding	Burst	Shock or vibration	Catastrophic	D	1	12 years
	Leak	Attack of materials or welds	Loss of propellant	C	2	

The analyses give for each failure mode the probable causes, the probable effects, and estimates of relative likelihood (A highest, E lowest) and degree of technical challenge (5 highest, 1 lowest). In addition, an estimate is given of the total number of years of development after the first flight hardware possible by 1972, the assumed cutoff date for decisions related to this study.

FAILURE RATE ASSESSMENT

The method presented in the preceding analysis was used to derive the set of failure rates in Table A-6. Tables A-7 and A-8 present the actual work sheets used for deriving these failure rates to enable examination of judgements and assumptions made. The year given in the "year operational" column is defined as the earliest date at which flight hardware was or could be available but not necessarily actually flown. Examination of the tables indicates that sufficient development time would be available by 1972 to hold failure rates within reasonable limits on all systems.

FAILURE RATE SUMMARY BY FAILURE MODE

The failure rates derived in the previous subsection may be subapportioned to the various failure modes by the failure modes and effects analysis. This step is necessary to obtain a system reliability prediction for the different failure modes of the same component which may have different effects on the system. The failure modes are partitioned according to the four classes delineated early in this appendix (see <u>The General System Model</u>).

Tables A-9 through A-13 present the subapportionments by failure mode for the individual systems.

TABLE A-6. SUMMARY FAILURE RATE ASSIGNMENT FOR USE IN 1972

(All failure rates percent/1000 hours unless otherwise noted)

System Component	Nitrogen		Hydrazine-Plenum		Hydrazine Liquid		Hydrazine Electrolysis		H_2O Electrolysis		NH_3 Vapor	
	Lower	Upper	Lower	Upper	Lower	Upper	Lower	Upper	Lower	Upper	Lower	Upper
Bladder	None		$300/10^6$ cy	$600/10^6$ cy	None		None		None		None	
Electrolysis cell	None		None		None		0.2000*	0.0500*	0.2000	0.0500	None	
Combustion chamber (including catalyst bed)	None		$5/10^6$ cy**	$15.8/10^6$ cy**	$5/10^6$ cy**	$15.8/10^6$ cy**	None		None		None	
Filters	0.001	0.003	0.001	0.034	0.001	0.003	0.001	0.006	0.001	0.006	0.001	0.003
Heater	None		None		None		None		None		0.008	0.032
Nozzles	0.000	0.000	0.000	0.000	0.000	0.000	0.000	0.000	0.000	0.000	0.000	0.000
Manifolds	0.025	0.055	0.025	0.055	0.025	0.055	0.025	0.055	0.025	0.055	0.025	0.055
Pressure switches (or temperature switch for NH_3)	None		$0.14/10^6$ cy	$0.14/10^6$ cy	$0.14/10^6$ cy	$0.14/10^6$ cy	$0.14/10^6$ cy	$0.14/10^6$ cy	$0.14/10^6$ cy	$0.14/10^6$ cy	$0.07/10^6$ cy	$0.07/10^6$ cy
Valves control (single valve)	0.227	0.474	0.227	0.762	0.227	0.487	0.227	0.569	0.227	0.569	0.227	0.513
Fill	0.010	0.020	0.010	0.020	0.010	0.020	0.010	0.020	0.010	0.020	0.010	0.020
Flow	None		0.227	0.489	None		None		None		None	
Relief	0.024	0.106	0.024	0.688	None		0.024	1.270	0.024	0.935	0.024	0.259
Regulators	0.070	0.143	None		None		0.070	0.200	0.070	0.200	0.070	0.171

*Estimate only, since part count of electronics required has not been determined.

**Derived from estimate of Hamilton Standard Division, United Aircraft Corporation. Small amount of actual data available

TABLE A-7. FAILURE RATE WORKSHEET FOR VALVES (1972 designs)

System	Adhesion	Freezing Shut	Armature Sticking	Spring Failure	Dirt on Seat	Seal Leak	High Temperature Stick Open	Corrosion	Attack of Elastomers	Total	Year Operational	T Years (to 1972)	$U^{(o)}$ ($x10^{-5}$)	e^{-KT}	Final Upper Limit, $U^{(T)}$, 10^{-5} fail/hr	Final Lower Limit, L, 10^{-5} fail/hr
Nitrogen	1	1	1	1	1	2	0	1	1	9	1960	12	1.15	0.21	0.474	0.227
Hydrazine plenum hot gas (roll control)	1	5	1	2	10	10	5	6	0	24	1968	5	1.97	0.200	0.762	0.227
No roll control	1	3	1	1	4	4	0	3	3		1968					
Flow valve	1	1	1	1	1	1	0	3	3	13	1961	11	1.28	0.033	0.487	0.227
Hydrazine liquid (dual system)	1	2	1	1	1	1	0	4	3	14	1961	11	1.34	0.033	0.489	0.227
Hydrazine electrolysis	1	1	1	1	1	4	0	1	1	11	1966	6	1.21	0.276	0.569	0.227
Ammonia	1	1	1	1	1	1	0	2	3	11	1964	8	1.15	0.077	0.513	0.227
Water electrolysis	1	1	1	1	1	4	0	1	1	12	1966	6	1.21	0.145	0.569	0.227

For valves

$L = 0.227$

$U = 1.97$

$K = 0.321 \qquad (e^{-KT} = 0.2)$

$$U^{(T)} = 2L + (U^{(o)} - 2L)e^{-KT} = 2L(1 - e^{-KT}) + U^{(o)}e^{-KT}$$

TABLE A-8. FAILURE RATE WORKSHEETS FOR COMPONENTS

Item and System	Failure Mode			Total Failures	Year Operational (T)	$\lambda_U(o)$	$\lambda_U(T)$	λL
	Burst	Leak						
Tanks						0.027		0.008
Nitrogen	2	2		4	1960 (12)	0.022	0.016	0.008
Hydrazine plenum	4	4		8	1968 (4)	0.027	0.018	0.008
Dual hydrazine	3	3		6	1967 (5)	0.024	0.018	0.008
Hydrazine electrolysis	3	3		6	1967 (5)	0.024	0.018	0.008
Water electrolysis	3	3		6	1964 (8)	0.024	0.017	0.008
Ammonia	1	1		2	1964	0.019	0.016	0.008
	Passage Contaminant	Discontinuity	Clogging					
Filters						0.162		0.001
Nitrogen	1	1	1	3	1960 (12)	0.046	0.003	0.001
Hydrazine plenum								
Hot gas	5	1	5	11	1967	0.162	0.034	0.001
Cold gas	1	1	1	3	1961 (11)	0.046	0.003	0.001
Hydrazine electrolysis	1	1	2	4	1964 (8)	0.060	0.006	0.001
Hydrazine liquid (dual)	1	1	1	3	1961 (11)	0.046	0.003	0.001
	Burst	Leak	Catalyst Attrition					
Combustion Chamber and Catalyst Bed						$50/10^6$ cy*		$5/10^6$ cy*
Hydrazine plenum	1	1	3	5	1966 (6)	50	15.8	5
Hydrazine liquid (dual) (in thruster)	1	1	3	5	1966 (6)	50	15.8	5
NH_3	1	1	0	2	1964 (8)	26	11.2	5
Pressure switches	All equal					$0.14/10^6$ cy	$0.14/10^6$ cy	$0.14/10^6$ cy
Electrolysis cells	No comparison made						0.0500	0.0250**
Heater ammonia	No comparison made				1960 (12)	0.767 0.767	0.032	0.008 0.008
	Leak	Burst						
Manifolding***	4	1		5	1960 (12)	0.485 0.398	0.057	0.025 0.025

*Data from Hamilton Standard Division, United Aircraft Corporation.

**Failure rate due essentially to power supply.

***Assumed same for all systems.

Table A-8 (continued)

Item and System	Failure Mode				Total Failures	Year Operational (T)	$\lambda_U(o)$	$\lambda_U(T)$	λL
	Adhesion	Armature Sticking	Spring	Seal Leak					
*Relief Valves							3.25		0.024
Nitrogen	1	1	1	3	6	1960 (12)	2.79	0.106	0.024
Hydrazine plenum	3	1	2	1	7	1967 (5)	3.25	0.688	0.024
Hydrazine electrolysis	1	1	1	4	7	1969 (3)	3.25	1.27	0.024
Water electrolysis	1	1	1	4	7	1968 (4)	3.25	0.935	0.024
Ammonia	2	1	1	3	6	1964 (8)	2.79	0.259	0.024
Nozzles	All assumed λ = 0.000 since failure results in degradation only.								
Squib valve Nitrogen							Reliability estimate 0.999 1 cycle		0.9995 1 cycle
	Stick Open	Stick Shut	Leak						
Regulator							0.554		0.070
Nitrogen	2	3	4		9	1960	0.453	0.146	0.070
Ammonia	2	4	5		11	1964	0.554	0.171	0.070
Electrolysis bladder	2	3	6		11	1966	0.554	0.200	0.070
Hydrazine plenum						1961	300 fail/ 10^6 cy		300 fail/ 10^6 cy

*Relief valves will have minor effect on system reliability as they do not operate unless another failure occurs.

TABLE A-9. NITROGEN SYSTEM FAILURE RATE APPORTIONMENT

Component	Failure Mode	Effect*	Failure Rate, percent/1000 hr	
			Upper	Lower
Tank	Burst	C	0.008	0.004
	Leak	MO	0.008	0.004
Fill valve (1)	Leak	MO	0.000	0.000
Filters	All	MC	0.003	0.001
Manifolding	Leak	MO	0.0456	0.0200
	Burst	C	0.0114	0.0050
Relief valve	Negligible contribution to failure rate			
Squib valve	Closed	MC	% - 0.999	R = 0.9995
Regulator	Open	MO	0.098	0.047
	Closed	MC	0.049	0.023
Control valves	Closed	S	0.154	0.076
	Full open	S	0.102	0.050
	Open	S	0.307	0.151

*C ≡ catastrophic

MO ≡ major open

MC ≡ major closed

S ≡ special

TABLE A-10. AMMONIA SYSTEM
FAILURE RATE APPORTIONMENT

Component	Failure Mode	Effect	Failure Rate, percent/1000 hr	
			Upper	Lower
Control valve	Closed	S	0.177	0.083
	Full open	S	0.088	0.041
	Open	S	0.309	0.144
Tanks	Leak	MO	0.008	0.004
	Burst	C	0.008	0.004
Fill valves				
Filters		MC	0.003	0.001
Temperature switch	Too little gas generation	MC	$0.035/10^6$ cy	$0.035/10^6$ cy
	Too much gas generation	MO	$0.035/10^6$ cy	$0.035/10^6$ cy
*Batteries		MC	R = 1.000	
Solar cells		MC	R = 1.000	
Heaters		MC	0.032	0.008
Manifolding	Leak	MO	0.0456	0.0200
	Burst	C	0.0114	0.0050
Regulator	Open	MO	0.109	0.045
	Closed	MC	0.062	0.025
Heat chamber	Burst	C	$5.6/10^6$ cy	$2.5/10^6$ cy
	Other	MC	$5.6/10^6$ cy	$2.5/10^6$ cy

*Two extra cells, 0.05 percent/1000 hr per cell; reliability is essentially 1.0.

TABLE A-11. HYDRAZINE PLENUM FAILURE RATE APPORTIONMENT

Component	Failure Mode	Effect	Failure Rate, percent/1000 hr	
			Upper	Lower
Control valves (no roll control)	Closed	S	0.232	0.079
	Full open	S	0.066	0.023
	Open	S	0.431	0.148
Control valves (roll control)	Closed	S	0.262	0.068
	Full open	S	0.175	0.045
	Open	S	0.617	0.159
Tanks	Burst	C	0.009	0.004
	Leak	MO	0.010	0.004
Filters				
Hot	All	MC	0.046	0.001
Cold	All	MC	0.003	0.001
Manifolding	Leak	MO	0.0456	0.0200
	Burst	C	0.0114	0.0050
Bladder (one cycle)	All	MC	$300/10^6$ cy	$300/10^6$ cy
Pressure switch	Too much gas generation	MO	$0.07/10^6$ cy	$0.07/10^6$ cy
	Too little gas generation	MC	$0.07/10^6$ cy	$0.07/10^6$ cy
Flow valve	Open	S, MO	0.272	0.132
	Closed	S, MC	0.195	0.095
Combustion chamber and catalyst bed	Burst	C	$3.16/10^6$ cy	$1/10^6$ cy
	Other	MC	$12.64/10^6$ cy	$4/10^6$ cy
Relief valves	Negligible effect on system reliability			

TABLE A-12. HYDRAZINE OR WATER ELECTROLYSIS FAILURE RATE APPORTIONMENT

Component	Failure Mode	Effect	Failure Rate, percent/1000 hr	
			Upper	Lower
Control valve	Closed	S	0.140	0.062
	Full open	S	0.093	0.041
	Open	S	0.374	0.165
Tanks	Burst	C	0.009	0.004
	Leak	MO	0.008	0.004
Fill valves				
Filters	All	MC	0.006	0.001
Electrolysis cell	All	MC	0.0500	0.62500
Solar cells		MC	R =	R =
Pressure switches (2 cycles/day)	Too little gas generation	MC	$0.07/10^6$ cy	$0.07/10^6$ cy
	Too much gas generation	MO	$0.07/10^6$ cy	$0.07/10^6$ cy
Manifolding	Leak	MO	0.0456	0.0200
	Burst	C	0.0114	0.005
Relief valve	Negligible effect on system reliability			
Regulator	Open	MO	0.145	0.051
	Closed	MC	0.055	0.019

TABLE A-13. DUAL HYDRAZINE FAILURE RATE APPORTIONMENT

Component	Failure Mode	Effect	Failure Rate, percent/1000 hr	
			Upper	Lower
Common Elements				
Tank	Burst	C	0.009	0.004
	Leak	MO	0.008	0.004
Manifolding (2 sets)	Leak	MO	0.0456	0.0200
	Burst	C	0.0114	0.0050
Relief valve	Negligible effect on system reliability			
Gas Side				
Electrolysis cell	All	MC	0.0500	0.0250
Pressure switches	Too little gas generation	MC	$0.07/10^6$ cy	$0.07/10^6$ cy
	Too much gas generation	MO	$0.07/10^6$ cy	$0.07/10^6$ cy
Control valves	Closed	S	0.0140	0.062
	Full open	S	0.093	0.041
	Open	S	0.374	0.165
Filter	All	MC	0.006	0.001
Liquid Side				
Filter	All	MC	0.003	0.001
Control valves	Closed	S	0.195	0.095
	Full open	S	0.078	0.038
	Open	S	0.272	0.132
Catalyst and combustion chamber (36 cy/valve)	Decomposition	MC, S	$15.8/10^6$ cy	$5/10^6$ cy
	Others			
Solar cells, R = 1.00				

THE GENERAL RELIABILITY FORMULA

A general reliability formula for computing any of the previously modeled systems is given here.

The following symbols and variables will be used:

K_1 = 2 for redundant plenum system
1 for single system

K_2 = 2 for quad or dual valve
1 for single valve

K_3 = 1 for dual system with less than 3 times required fuel
0 otherwise

K_4 = 2 for quad valves
1 otherwise

N = number of valves per plenum

T_c = critical time

T = time, hours

Failure rates:

λ_{VF} = single valve, full open

λ_{VO} = single valve open (leak or full open)

λ_{Vc} = single valve closed

λ_{MO} = total major open failure modes except for valves

λ_{MC} = total major closed failure modes except for valves

λ_{C} = total critical failure modes

Define the following quantities:

$$R_{Vf}(T) = \left[1 - \left(1 - e^{-\lambda_{Vf}T}\right)^{K_2}\right]^{K_4}$$

$$R_{VO}(T) = \left[1 - \left(1 - e^{-\lambda_{VO}T}\right)^{K_2}\right]^{K_4}$$

$$R_{VC}(T) = 1 - \left(1 - e^{-K_2 \lambda_{VC} T}\right)^{K_4}$$

$$R_{MO}(T) = R_{VO}^{\;N}(T) e^{-\lambda_{MO} T}$$

$$\lambda_{OM}(T) = \lambda_{MO} - \frac{N \ln R_{VO}(T)}{T}$$

$$R_{MC}(T) = e^{-\lambda_{MC} T}$$

The total system reliability is the product of the following terms

$$\left\{R_{VC}(T_c)\left[1 - (1 - R_{Vf})(T - T_c)\right]\left[1 - R_{VC}(T)\right]\right\}^{NK_1 K_3}$$

A correction factor for not having three times required fuel is: (equal to 1 if $K_3 = 0$).

$$\left\{1 - \left[1 - R_{Vf}(T)\right]\left[1 - R_{Vc}(T)\right]\right\}^{2N(1-K_3)(K_1-1)}$$

Correction for dual failure even if three times required fuel is carried (see Case 1)

$$\Big\{ e^{-2\lambda_c T}\left[1 - 1 - R_{VC}(T)^{\,2}\right]^{N} R_{MO}^{\;2}(T) R_{MC}^{\;2}(T)$$

$$+ e^{-\lambda_c T} R_{VC}^{\;N}(T) \frac{0.2\,\lambda_{OM}(T)}{\lambda_c + \lambda_{OM}(T) + \lambda_{MC}} R_{MO}(T) R_{MC}(T)\left|1 - R_{MO}(T) R_{MC}(T) R_C(T)\right|$$

$$+ e^{-2\lambda_c T} R_{VC}^{\;N}(T) \cdot \frac{2\lambda_{MC}}{\lambda_{OM}(T) + \lambda_{MC}} R_{MO}(T) R_{MC}(T)\left[1 - R_{MO}(T) R_{MC}(T)\right]\Big\}^{(K_1-1)}$$

for redundant systems. (Note: This statement equals 1 if $K_1 = 1$.)

$$\left[e^{-\lambda_c} R_{MO}(T) R_{MC}(T) R_{VC}^{\;N}(T)\right]^{(2-K_1)}$$

for single systems. (Note: This statement equals 1 if $K_1 = 2$.)

This general reliability formula has been programmed for machine calculation.

SPECIAL RELIABILITY CONSIDERATIONS IN DUAL MODE HYDRAZINE SYSTEM ANALYSIS

The dual mode hydrazine system differs in a fundamental way from the others considered, in that it is in essence two subsystems — a liquid hydrazine and an electrolysis system (without a regulator) operating off common tankage. A failure in either subsystem clearly can influence the operation of the other.

For a single plenum system, it is sufficient to consider the dual mode hydrazine system as two independent plenums and thus multiply the reliabilities of the gas side and liquid side together to obtain the dual mode hydrazine reliability. For the two plenum system, however, this assumption is not valid. To simplify the calculations, however, it was convenient to make this assumption of independence for the plenum system and apply a correction factor, as discussed below.

The effect of this assumption is shown in Table A-14, in terms of the redundancy of the system remaining following a single failure. Thruster redundancy is the only parameter affected by the assumption. It is seen that the only differences in the two models result if major open failures occur.

TABLE A-14. COMPARISON OF MODELS FOR DUAL MODE HYDRAZINE SYSTEM

	Effect on Thruster Redundancy*			
	Rigorous Model		Independent Model	
Event	Gas Side	Liquid Side	Gas Side	Liquid Side
No major failures	R	R	R	R
Major open (gas)	NR	NR	NR	R
Major open (liquid)	NR	NR	R	NR
Major closed (gas)	NR	R	NR	R
Major closed (liquid)	R	NR	R	NR

*R ≡ redundant
NR ≡ nonredundant

If ${}_{\ell}P_{MO}$ and ${}_{g}P_{MO}$ are the probabilities of major open failures for the gas and liquid sides respectively, if there are N thrusters per plenum, and if ${}_{\ell}R_T$ and ${}_{g}R_T$ are the thruster reliabilities (closed mode), the numerical effect on system reliability may be computed as follows:

The reliability of the thrusters, if a major open (gas) failure occurs, is, under the rigorous model:

$${}_{\ell}R_T{}^N \cdot {}_{g}R_T{}^N$$

while under the independent model, it is

$${}_{g}R_T{}^N \left[1 - (1 - {}_{\ell}R_T)^2\right]^N$$

The difference is

$${}_{g}R_T{}^N \left|\left[1 - (1 - {}_{\ell}R_T)^2\right]^N - {}_{\ell}R_T{}^N\right|$$

The impact on the system reliability of changing models is then

$$E_g = {}_{g}P_{MO}\, {}_{g}R_T{}^N \left\{\left[1 - (1 - {}_{\ell}R_T)^2\right]^N - {}_{\ell}R_T{}^N\right\}$$

for the gas side, and similarly for the liquid failure,

$$E_{\ell} = {}_{\ell}P_{MO}\, {}_{\ell}R_T{}^N \left\{\left[1 - (1 - {}_{g}R_T)^2\right]^N - {}_{g}R_T{}^N\right\}$$

Assuming values of R_T and ${}_{g}R_T$ close to 1 and ignoring second order effects,

$$R_R = R_I - E_{\ell} - E_g$$

where R_R is the reliability using the rigorous model and R_I is the reliability using the "independent" model.

Thus the original "independent" model computer program was used to compute R_I and the above equation was used to "convert" to R_R. A sample calculation showed that E_g and E_{ℓ} were both on the order of 10^{-4} for the quad valve systems and thus could be neglected. For single valve systems, however, the effect was non-negligible and thus the numbers appearing in Table A- have been corrected to include E_g and E_ℓ.

RESULTS

Table A-15 gives the results of the calculations of upper and lower limit system reliabilities.

CONCLUSIONS AND RECOMMENDATIONS

Comparison of Configurations and Systems

The computations summarized in Table A-15 permit establishment of the following preferences on a reliability basis. Irrespective of the type of system chosen, the following order is preferred:

1) Redundant plenums with quad or dual valves

2) Single plenums with quad or dual valves

3) Redundant plenums with single valves

Within these configurations, preference may be established as follows:

1) Redundant plenums with quad or dual valves

    a) Nitrogen

    b) Electrolysis

    c) Dual mode hydrazine

    d) Vaporjet

    e) Hydrazine plenum

    If it is decided to use this configuration, reliability would not be the key factor in chosing the optimum system because all are adequate.

2) Single plenums, quad or dual valves

    a) Nitrogen

    b) Vaporjet

    c) Electrolysis

TABLE A-15. SYSTEM RELIABILITIES

System Number	Number of Plenums	Valve Configuration	Roll Control	Number of Valves per Plenum	Total Impulse, I_T/I_T, Required	Cold Gas		Vaporizing Liquid (Battery)		Hydrazine Catalytic Plenum		Vaporizing Liquid (Regenerative or External Heat Exchanger)		Water or Hydrazine Electrolysis		Dual Mode Hydrazine*	
						Lower	Upper	Lower	Upper	Lower	Upper	Lower	Upper	Lower	Upper	Lower	Upper
1a	1	Quad (redundant)	No	12	1.0	0.958	0.984	0.951	0.983	0.939	0.989	0.945	0.980	0.939	0.982	0.934	0.981
1b	1	Dual (redundant)	No	24	1.0	0.958	0.984	0.951	0.983	0.939	0.989	0.945	0.980	0.939	0.982	0.934	0.981
1c	1	Quad (redundant)	Yes	16	1.0	0.951	0.983	0.944	0.981	0.877	0.936	0.939	0.979	0.930	0.980	0.919	0.978
1d	1	Dual (redundant)	Yes	32	1.0	0.951	0.983	0.944	0.981	0.877	0.936	0.939	0.979	0.930	0.980	0.919	0.978
2a	2	Quad	No	6	2.0	0.995	0.998	0.991	0.996	0.991	0.997	0.990	0.996	0.994	0.998	0.992	0.997
2b	2	Dual (redundant)	No	12	2.0	0.995	0.998	0.991	0.996	0.991	0.997	0.990	0.996	0.994	0.998	0.992	0.997
2c	2	Quad	Yes	8	2.0	0.994	0.998	0.990	0.996	0.985	0.997	0.989	0.996	0.993	0.998	0.991	0.997
2d	2	Dual (redundant)	Yes	16	2.0	0.994	0.998	0.990	0.996	0.985	0.997	0.989	0.996	0.993	0.998	0.991	0.997
3a	2	Single	No	6	3.0	0.936	0.982	0.931	0.981	0.870	0.977	0.929	0.980	0.919	0.981	0.846	0.960
3b	2	Single	Yes	8	3.0	0.902	0.971	0.896	0.971	0.725	0.965	0.893	0.970	0.878	0.970	0.758	0.935
4a	1	Quad	No	6	1.0	0.962	0.986	0.955	0.983	0.947	0.989	0.949	0.981	0.950	0.984	0.944	0.983
4b	1	Dual (redundant)	No	12	1.0	0.962	0.986	0.955	0.983	0.947	0.989	0.949	0.981	0.950	0.984	0.944	0.983
4c	1	Quad	Yes	8	1.0	0.957	0.984	0.949	0.982	0.908	0.988	0.943	0.980	0.943	0.982	0.933	0.981
4d	1	Dual (redundant)	Yes	16	1.0	0.957	0.984	0.949	0.982	0.908	0.988	0.943	0.980	0.943	0.982	0.933	0.981

*Assumes liquid and gas valves have entirely separate functions.

d) Hydrazine plenum

e) Dual mode hydrazine

This order holds for the case with no added roll control only. For the added roll control case, the hydrazine plenum system is least attractive.

3) Dual plenums, single valves

a) Nitrogen

b) Vaporjet

c) Electrolysis

d) Dual mode hydrazine

e) Hydrazine plenum

If this configuration were chosen, the low reliabilities (lower bounds) of the dual mode hydrazine and hydrazine plenum systems should eliminate them as effective contenders because of the criticality of valve reliability in this case.

It must be emphasized that although the large spread of uncertainty between the upper and lower reliability numbers indicates, for example, that a particular hydrazine plenum system could have a higher reliability than a particular nitrogen system, it cannot be said that the systems are of equal reliability. Certainly the low lower bound of the hydrazine plenum system indicates it is a greater risk.

As seen in Table A-15, in addition to the three basic configurations, a fourth was considered. This is essentially configuration 1 with the exception that there is only one quad valve thruster (or redundant pair of dual valves) per function. This alternative should be considered because the failure mode analysis indicates that open failure (includes excessive leak) is more likely than closed failure and thus any extra thrusters added for redundancy pose more of a hazard than a safeguard. Configuration 4 has, therefore, according to this hypothesis a better reliability than configuration 1.

General Conclusions

1) The choice of configuration is more important than the choice of system (e.g., nitrogen versus electrolysis) if at least 4 years of development time is available.

2) Any of the systems considered would have high (0.99) reliability, if redundant plenums and quad valves were used for a 1972 launch. The hydrazine plenum system with roll control does stand out, however, as a considerably larger risk than the others.

3) The final choice of optimum system will depend upon the value placed upon weight reduction. Two possible approaches for selection are:

   a) Change both weight and reliability to a common denominator — e.g., dollars.

   b) Change weight saving into reliability gain. Spare weight would then be used to add redundancy to other areas of the spacecraft to improve total mission reliability.

   Both of these approaches require knowledge of a definite spacecraft and mission, as the actual value of weight saved varies greatly from spacecraft to spacecraft and mission to mission, and can be either negligible or critical.

Recommendations

The following are recommended as areas for further effort:

1) Further investigation into the independence of failures for the quad valve configuration.

2) Investigation into the possibility of quad redundant regulators.

3) Definition of a specific spacecraft and mission so that the choice of optimum system could be made. As an exercise, this could be performed for some existing system such as Mariner.

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APPENDIX B. COLD GAS SYSTEMS

SUMMARY

A detailed study was performed on a nitrogen cold gas control system to establish baseline designs for performance and reliability comparison with the other propellants and systems being studied. Based on control system performance requirements of impulse bit size and thrust levels, the operating parameters were optimized and propellant tank design criteria and baseline designs established. System component design parameters, weights, and power requirements were defined. The transient response characteristics of the control systems were evaluated for the case in which the thrust impulse period is small compared with minimum practical thrust chamber and control valve time delays.

The baseline design system is shown in Figure 2; Table B-1 defines individual component weights. Typical components are listed in Table B-2.

An alternative propellant, Freon 14 (carbon tetrafluoride), was also examined. Freon 14 has high density and favorable compressibility at 1500 psi, allowing a substantial reduction in tank weight. The lower tank weight more than compensates for a somewhat inferior specific impulse.

The Freon 14 calculations were performed in the same way as those for the nitrogen system. It is necessary to recognize, however, that highly compressible gases have an interesting characteristic not usually important for ordinary gases: a large Joule-Thompson effect. This effect, an unwanted cooling upon expansion through a regulator, results in up to about 5 percent loss in performance for steady-state thrusting with Freon 14. Low duty cycle pulses are relatively unaffected because ambient heat soakback may replace the lost thermal energy. The Freon 14 system weights shown in Tables B-3 and B-4 were not compensated for Joule-Thompson effect but, as a consequence of the particular system design, they still represent a fair comparison of propellant capability. The original designs used the same conservative pressure regulator that limited minimum tank pressure to 200 psi. However, storage of nitrogen at 3500 psi and Freon 14 at only 1500 psi, combined with the higher Freon density, actually resulted in an unnecessary penalty to the Freon system in the amount of residual gas. By requiring a lower pressure drop across the regulator (easily possible with state-of-the-art regulators), more than enough wasted propellant could be recovered to compensate for Joule-Thompson losses.

TABLE B-1. NITROGEN GAS SYSTEM COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Component	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Fixed weight															
Fill valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Control valve															
Cruise	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(43) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Retro	1.3	— —	— —	(16) 20.8	(16) 20.8	— —	— —	(16) 20.8	(16) 20.8	— —	(4) 5.2			(8) 10.4	(8) 10.4
Regulator (with relief valve)	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Thruster															
0.08 lb_f	0.06	(12) 0.7	— —	(12) 0.7	— —	(12) 0.7	— —	(12) 0.7	— —	(12) 0.7	(12) 0.7	(6) 0.4		(6) 0.4	
0.04 lb_f	0.06	— —	(24) 1.4	— —	(24) 1.4	— —	(24) 1.4	— —	(24) 1.4	— —	— —		(12) 0.7		(12) 0.7
0.83 lb_f	0.09	— —	— —	— —	(8) 0.7	— —	— —	— —	(8) 0.7	— —	— —				(4) 0.4
1.67 lb_f	0.13	— —	— —	(4) 0.5	— —	— —	— —	(4) 0.5	— —	— —	(4) 0.5			(2) 0.3	
Manifolds and brackertry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Total fixed weights		31.8	32.5	53.1	54	43.7	44.4	65.0	65.9	29.3	35	21.9	22.2	32.6	33
Propellant		54.0	54.0	87.0	87.0	108	108	174	174	162	261	54.0	54.0	87.0	87.0
Tankage		66.0	66.0	107	107	132	132	214	214	198	322	66.0	66.0	107	107
Total system weights		151.8	152.5	247.1	248	283.7	284.6	453	454	389.3	618	141.9	142.2	226.6	227

TABLE B-2. NITROGEN COLD GAS ATTITUDE CONTROL SYSTEM TYPICAL COMPONENTS

Component	Description	Operating Pressure, psi	Material(s)	Envelope, inches	Flow Rate(s), lb/sec	Leak Rate, std cc/hr	Equivalent Orifice (or Throat Diameter), inches	Input Power Required	Weight, pounds
Propellant tank	Hydroformed and welded spherical shell	3500	Titanium alloy 6Al, 4V	Variable	Not Applicable	0	Not applicable	Not applicable	
Fill and vent valve	Two stage, pilot operated, spring loaded and quick disconnect fill line with cap	3500 Relief 3250 ± 50	Stainless steel with Buna N or Viton soft seats	1-1/2 by 2 by 3/4	0.10	0	Not applicable	Not applicable	1.30
Start valve	Pyrotechnic operated dual squib		Aluminum valve body	1-1/2 by 2 by 3/4	0.05	0	0.18	28 volts 9.0 amperes for 10 milliseconds	1.0
Filter	Stainless steel wire mesh 10 microns 25 micron absolute	5250 yield 7000 burst	Stainless steel mesh and housing	2 inches diameter by 2 inches long	0.05	0		Not applicable	0.6 (estimated)
Pressure regulator	Single stage, dome loaded	Inlet: 3500 to 200 Outlet: 50 ± 1	Cast anodized aluminum housing, Buna N elastomers	5 by 5 by 8	0.05	<4.0	~	Not applicable	5.0
Relief valve (integral with pressure regulator)	Spring loaded, soft seat	Crack 55 psig maximum. Reseat 52 psig minimum	Same as regulator	See pressure regulator	0.05 maximum	0.10 minimum	~	Not applicable	Included in regulator
Control valve (similar to Eckel AF53)	Direct acting, coaxial solenoid type	50 psig nominal 350 psig maximum overpressure	Stainless steel seat with Teflon or nylon poppet	1-1/8 diameter by 1-1/2 long	1.1×10^{-3}	<0.1 maximum	0.086	15 watts for 5 milliseconds minimum	0.4 per solenoid
Control valve (similar to Eckel AG 103)	Directing acting, coaxial solenoid type	50 psig nominal 350 psig maximum overpressure	Stainless steel seat with teflon or nylon poppet	1-15/16 diameter by 1-3/4 long	1.1×10^{-2}	0.3 maximum	0.22	33 watts	1.3 per solenoid
Thrust Nozzle									
0.04 pound thrust		50 psi	Aluminum alloy	1/2 by 5/8	5.5×10^{-4}	Not applicable	0.023	Not applicable	0.06
0.08 pound thrust	Conical expansion	50 psi	Aluminum alloy	1/2 by 3/4	1.1×10^{-3}	Not applicable	0.033	Not applicable	0.06
0.83 pound thrust	Nozzle with 16 degree half angle	50 psi	Aluminum alloy	1-1/8 by 2-1/2	1.1×10^{-2}	Not applicable	0.105	Not applicable	0.09
1.67 pound thrust		50 psi	Aluminum alloy	2-1/4 by 4-1/2	2.3×10^{-2}	Not applicable	0.150	Not applicable	0.13

TABLE B-3. FREON 14 GAS SYSTEM COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Component	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Fixed weight															
Fill valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Control valve															
Cruise	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Retro	1.3	— —	— —	(16) 20.8	(16) 20.8	— —	— —	(16) 20.8	(16) 20.8	— —	(4) 5.2			(8) 10.4	(8) 10.4
Regulator (with relief valve)	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Thruster															
0.08 lb_f	0.06	(12) 0.7	— —	(12) 0.7	— —	(12) 0.7	— —	(12) 0.7	— —	(12) 0.7	(12) 0.7	(6) 0.4		(6) 0.4	
0.04 lb_f	0.06	— —	(24) 1.4	— —	(24) 1.4	— —	(24) 1.4	— —	(24) 1.4	— —	— —		(12) 0.7		(12) 0.7
0.83 lb_f	0.09	— —	— —	— —	(8) 0.7	— —	— —	— —	(8) 0.7	— —	— —				(4) 0.4
1.67 lb_f	0.13	— —	— —	(4) 0.5	— —	— —	— —	(4) 0.5	— —	— —	(4) 0.5			(2) 0.3	
Manifolds and bracketry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Total fixed weights		31.8	32.5	53.1	54	43.7	44.4	65	65.9	29.3	35	21.9	22.2	32.6	33
Propellant		80.5	80.5	134.3	134.3	161	161	268.6	268.6	240.2	404	80.5	80.5	134.3	134.3
Tankage		24.6	24.6	41	41	49.2	49.2	82	82	73.4	123.4	24.6	24.6	41	41
Total system weights		136.9	137.6	228.4	229.3	253.9	254.6	415.6	416.5	342.9	562.4	127.0	127.3	207.9	208.3

TABLE B-4. SUMMARY OF OFF-THE-SHELF VALVE PERFORMANCE CHARACTERISTICS

Supplier	Part Number	Inlet Pressure, psi	Equivalent Orifice and Discharge Coefficient, inches	Nitrogen Flow at $\Delta P = 2$ psi	Power, watts	Weight, pounds	Leak Rate Closed, cc/hr	Seat Material	Time, milliseconds		Remarks
									Open	Close	
Eckel	BF63C-1	700	0.060, 0.65		15	0.50	0.10 at 700 psi	Nylon/303SS	5		
	AFL 77C-21	40	0.040, 0.65			0.20	0.10 at 40 psi	Teflon/430SS	7	5	
	AF56C-149A	1000Δ	0.016, 0.65		45	0.50	1 bubble each 15 minutes at 1000 psi	Nylon/303SS	15	15	
	AF102C-13	590	0.098, 0.65		30	1.0	1 at 550 psi	Kelf/300SS	20	20	
	AF53	50	0.125, 0.65		15	0.4	120 at 50 psi	Nylon/300 SS	15	8	
	AG103	1500	0.22, 0.65		33	1.3	0.3 (est) at 50 psi	Teflon/300SS	15 est	10 est	
Hughes Surveyor	235700-2			0.75 cfm at 25.5 psi		0.60	1 at 45 psi	Tungsten carbide	15.8	7.2	
ARPAT	X219940	600		63 cfm at 630 psi							
Parker	S413-B111	3000	0.12	0.29 scfm	25	1.3	Bubble tight per standard soap bubble method	Vespel SP I Polymide harder than aluminum	40 to 50	20.30	
	S413-C211	3000	0.12	0.29 scfm	25	1.9					
	S483-B111	3000	0.12	0.29 scfm	25	1.3					
	S483-C211	3000	0.12	0.29 scfm	25	1.9					
Saville											No response to date.
Skinner	V52LA2100 (EN) 28V	100	0.125	2.24 cfm	10	1.1	Bubble tight	Viton/SS	10 to 15	6 to 12	Not qualified.
	C2LA1092 (EN) 28V	90	0.125	2.5 cfm	8	1.0	Bubble tight	Synthetic brass	10 to 15	6 to 12	
Sterer	27-880	80+			20	0.75	4	Delrin/SS	25	25	
Valcor	62C32C3E	150	0.125	3.7 cfm	9	1.5		Buna N/brass	10	10	Not qualified.
	SV-11P52C4-5E (24 VOLT OPT)	125		5.0 cfm	10			Teflon/SS			
Valvair (Bellows)	PG-62-A 24 VOLT	90	0.187	2.68 cfm	8			Hycar			
	PC-42-A 24 VOLT	200	0.125	1.65 cfm	8			Hycar			
Weston	42C170	75 ± 5		12.5 cfm	Δ	0.61	None at 70 psi	Fluor silicon/SS			Δ28 volt, 2 ampere pulse to actuate, no hold required.
Whittaker	144785	50		1.87 cfm	2	0.18	7.2 to 18	Tungsten carbide/SS	11.8	5	

*Qualification test report.

NITROGEN SYSTEM BASELINE DESIGN

The design established is based on state-of-the-art components throughout; component performance values, weights, and power requirements are all based on fully developed and qualified components except for the propellant tanks and thrusters, which are generally sized for a specific design. No attempt has been made to engage in extensive theoretical analyses of maximum attainable component capability; instead, demonstrated performance has been used throughout.

Control Valves

The selection of control system impulse bit size is governed by the vehicle inertial characteristics (roll, yaw, and pitch axes moments of inertia) and the control accuracy and response requirements. The impulse bit size must be small enough to prevent limit cycling in excess of the required control band range and large enough to avoid loss of control from maximum disturbing loads or torques. The impulse bit size range considered in this study is 16×10^{-4} lb_f-sec minimum and 120×10^{-4} lb_f-sec maximum; the thruster force levels of interest are 0.04 to 0.08 pound. Greer* has reported nonrepeatable and erratic impulse bit performance in thruster tests when control valve opening time plus closing time approaches, or is greater than, the impulse bit pulse period. This effect is apparently due to nonrepeatability of valve opening and closing dynamics. Consequently, it is desirable to design for a pulse period of sufficient length to minimize the effects of the valve opening and closing transients on the total impulse bit.

Valve opening time is determined by a number of factors, including coil inductance, solenoid force and closing spring force, plunger friction, gas dynamic loads, valve plunger mass and stroke, and input power supply and control characteristics. The theoretical opening time (ignoring friction) for a simple solenoid valve arising from electrical and mechanical delays can be determined from the relationship

$$\Delta t_{vo} \cong \frac{L}{E} I_{min} + 1.4 \sqrt{\frac{SW}{(F_{sol} - F_{sp})g}} \qquad (1)$$

where

Δt_{vo} ≡ valve opening time, seconds

L ≡ valve coil inductance, henries

I_{min} ≡ valve current to overcome holding spring force, amperes

E ≡ voltage

*See references in this Section on Thruster Performance.

S ≡ valve stroke, feet

W ≡ valve plunger weight, pounds

F_{sol} ≡ solenoid force, pounds

F_{sp} ≡ valve closing spring force, pounds

g ≡ gravitational constant, ft/sec^2

Theoretically, solenoid valves weighing less than 1 pound can be designed for opening times of approximately 2 milliseconds; however, because a specific design will involve several compromises for the best balance of time response, flow characteristics, power input, weight, and control circuitry simplicity, a more practical criterion for determining valve dynamic characteristics is the state of the art in valve manufacture. Table B-4 is a brief summary of manufacturers' performance data on solenoid valves in the range of orifice size and type suitable for control systems in the size and pressure range being considered. From this survey, it is concluded that valve opening times of 5 to 7 milliseconds and closing times of 5 milliseconds are practical minima. It should be noted that with plunger position feedback power control circuitry, one manufacturer claims opening times of 2 milliseconds; however, an evaluation of this control circuitry reliability would be required to determine the desirability of using this approach. The nozzle sizing and operating conditions selected for this study are based on valve state of the art without feedback control.

The required valve orifice size criterion used was a maximum pressure drop of 2 psi at rated flow, or an area ratio of valve orifice to nozzle throat orifice of 2:1, to minimize thruster chamber pressure rise time. Although, in the systems utilizing 0.04 pound thrusters, such a valve has an oversized orifice for minimum pressure drop and minimum thrust chamber rise time, the valve size and weight are not appreciably reduced by a reduction in orifice size, because in very low response time valves the weight is due primarily to the large solenoid required to minimize the mechanical time constant (see Equation 1).

For a minimum average thruster force level of 0.08 pound and the minimum impulse bit size of 16×10^{-4} lb-sec, the minimum pulse period must be approximately 0.02 second. Consequently, with minimum thruster force level, the pulse period can be made approximately twice the sum of reasonable valve opening and closing times, which will reduce the nonrepeatable effects of valve opening dynamics, but not sufficiently to avoid consideration of the transient effects on actual impulse bit size. To minimize nonrepeatable transient effects on control system performance, low level thrusters should be used, and the minimum levels of 0.04 and 0.08 pound were used for the pulsed thrusters. An analysis of thruster performance in the small impulse bit size range, where impulse period is the same order of magnitude as valve opening and closing time and thruster pressure buildup and delay time, was initiated to evaluate control performance.

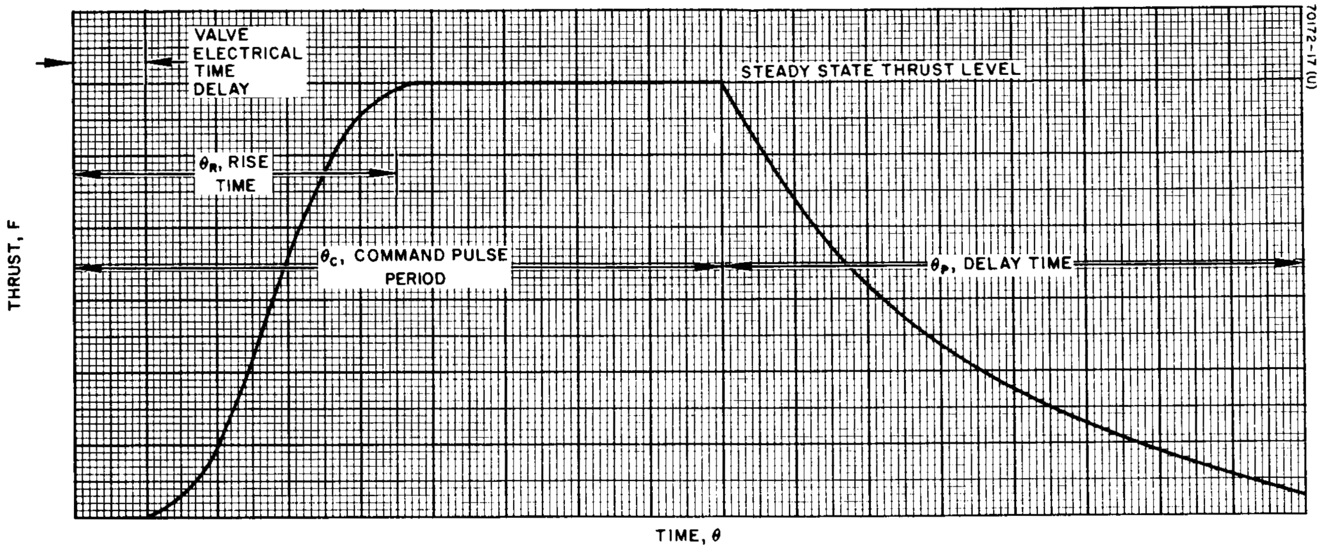


Figure B-1. Generalized Thrust Versus Time Function

Thruster Performance

By definition, the impulse for a given command is

$$I_c = \int_o^{\theta} F d\theta \tag{2}$$

where

I_c ≡ command impulse, lb-sec

F ≡ force of thruster, lbs

θ ≡ time from initiation of pulse command, seconds

Figure B-1 illustrates a typical thrust versus time history. The chamber pressure rise and decay times have been evaluated analytically by Greer and Griep* by treating the chamber buildup as an adiabatic process and the pressure decay as a polytropic expansion. Certain simplifications were made in this analysis. The valve electrical time delay was omitted because it is usually less than 1 millisecond for a well-designed valve, and will be different for any given system design; the effects of the variable valve orifice area and flow coefficient during valve opening and closing were also ignored. The valve electrical time delay effect can easily be included once a system design has been established, by simply adding the valve electrical delay to the total command pulse duration. The valve orifice opening and closing dynamics, however, produce effects not predictable by the analytical approach when the command pulse period is of the same order of magnitude as the valve opening and closing times.

For any system design that operates in the very short pulse period regime ($\Delta\theta_c \cong \Delta\theta_{vc}$ or $\Delta\theta_{vo}$), a more rigorous analytical and experimental study is necessary to predict thruster performance accurately. Within its limitations, however, the treatment of Greer and Griep has proved valid.

On the assumptions of vacuum operation, ideal gas behavior, constant density flow through the valve orifice,** and a sufficiently large ratio of orifice to nozzle throat areas, the treatment of Greer and Griep leads to

*H. Greer, "Analytical Investigation of Nitrogen Jet Reaction Control System", Aerospace Corporation, TDR-469 (5560-30)-1, 30 November 1964.

H. Greer and D.J. Griep, "Low Thrust Reaction Jet Performance", TDR-469 (5230-33)-2, August 1965.

H. Greer and D.J. Griep, "Dynamic Performance of Low Thrust Cold Gas Reaction Jets in a Vacuum", Aerospace Corporation, TR-669 (6230-33)-1, August 1966.

**This assumption agrees more closely with experiment than alternatives (see Greer and Griep, cited above).

$$P_{c_r} \cong P_s \left\{ 1 - (1+B)^2 e^{-2\tau} + 2B(1+B)\, e^{-\tau} - B^2 \right\} \Bigg]_0^{\theta = \theta_r} \tag{3}$$

$$P_{c_d} \cong P_s \left\{ 1 - \left[\frac{A_t a^*}{2V_c} \left(\frac{2}{\gamma+1} \right)^{\frac{\gamma+1}{2(\gamma-1)}} \right] (1-\gamma)\,\theta \right\}^{\frac{2\gamma}{(1-\gamma)}} \Bigg]_{\theta_{vc}}^{\theta = \theta_c} \tag{4}$$

where B and τ are defined as

$$B = \frac{A_o}{A_t} \left(\frac{2}{\gamma} \right)^{\frac{1}{2}} \left(\frac{\gamma+1}{2} \right)^{\frac{\gamma+1}{2(\gamma-1)}}$$

$$\tau = \log_e \left[\frac{\left(1 - \frac{P_{c_r}}{P_s} \right)^{\frac{1}{2}} + B}{1 + B} \right]$$

and:

P_{c_r} ≡ chamber pressure during buildup, psi

P_s ≡ steady state chamber pressure, psi

A_o ≡ area of control valve orifice, square inches

A_t ≡ area of thruster throat, square inches

γ ≡ ratio of specific heats or polytropic exponent

P_{c_d} ≡ chamber pressure during decay, psi

V_c ≡ volume of thruster chamber, cubed inches

a^* ≡ sonic velocity at throat conditions, in./sec

A_e ≡ area of nozzle cone exit, square inches.

θ_c ≡ time of end of command pulse

θ_r ≡ time during chamber pressure rise

θ_{vc} ≡ time of valve closing

The thruster force output as a function of chamber pressure is

$$F = \left[\left\{\frac{2\gamma^2}{\gamma - 1}\left(\frac{2}{\gamma + 1}\right)^{\frac{\gamma+1}{\gamma-1}}\left[1 + \left(\frac{P_e}{P_c}\right)^{\frac{\gamma-1}{\gamma}}\right]\right\}^{\frac{1}{2}} + \left(\frac{P_e}{P_c}\right)\frac{A_e}{A_t}\right] A_t P_c \qquad (5)$$

where (P_e), the exhaust plane pressure, is

$$P_e = P_c\left(1 + \frac{\gamma - 1}{2} M_e^{\,2}\right)^{\frac{-\gamma}{\gamma-1}}$$

and (M_e), the exit Mach number, is

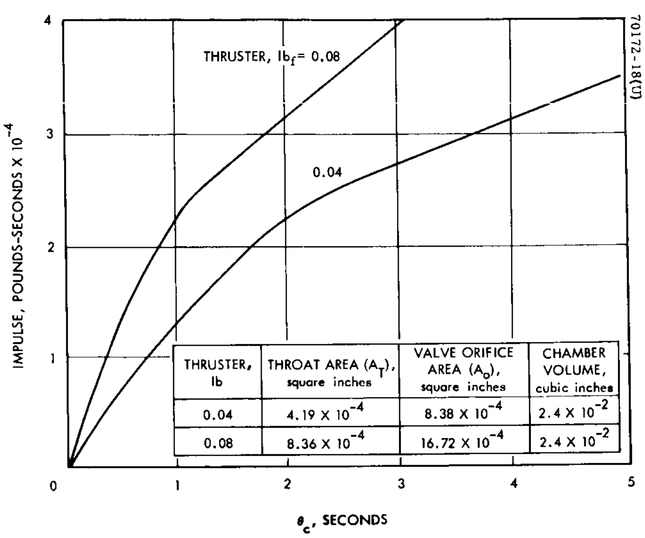
$$M_e \cong \frac{(\gamma + 1)A_e^{\frac{2(\gamma-1)}{\gamma+1}} + \left[(\gamma + 1)^2 A_e^{\frac{4(\gamma-1)}{\gamma+1}} - 8A_t^{\frac{4(\gamma-1)}{\gamma+1}}\right]^{\frac{1}{2}}}{2A_t^{\frac{2(\gamma-1)}{\gamma+1}}} \qquad (6)$$

Equations 3, 4, and 5 can be substituted into Equation 1 to yield a solution for impulse as a function of command pulse width. By use of computer techniques, impulse versus command pulse width was determined for each pulse mode thruster used in the baseline nitrogen system designs. Figure B-2 gives the predicted baseline design performance for pulsed operation and includes a summary of the nozzle design parameters used; it is apparent that thruster output is nonlinear in the short pulse period range and the control system programming will have to be designed to compensate for this effect.

The selection of thruster operating pressure involves a tradeoff between high pressures (which result in prohibitively small nozzle throat diameters with problems of holding very small tolerances, dirt sensitivity, and precision of blending contours) and larger size valves. Figure B-3 illustrates nozzle size as a function of thrust level for chamber pressures from 10 to 65 psia exhausting to vacuum. Throat diameters less than 0.020 inch were considered less practical for production components and so 50 psia was used, since for the smallest thruster (0.04 pound) the throat diameter would be 0.023 inch. No significant compromise is incurred in operating the remaining thrusters at 50 psi, and all systems designs are based on a nominal 50 psi chamber pressure.

Nozzle Performance

The reduction in nozzle efficiency at low thrust levels was examined; the results are given in Appendix I. Specific impulse was determined for thrust levels of 0.01 to 1.0 pound with and without condensation and with



THRUSTER, lb	THROAT AREA (A_T), square inches	VALVE ORIFICE AREA (A_o), square inches	CHAMBER VOLUME, cubic inches
0.04	4.19×10^{-4}	8.38×10^{-4}	2.4×10^{-2}
0.08	8.36×10^{-4}	16.72×10^{-4}	2.4×10^{-2}

Figure B-2. Impulse Versus Command Pulse Width

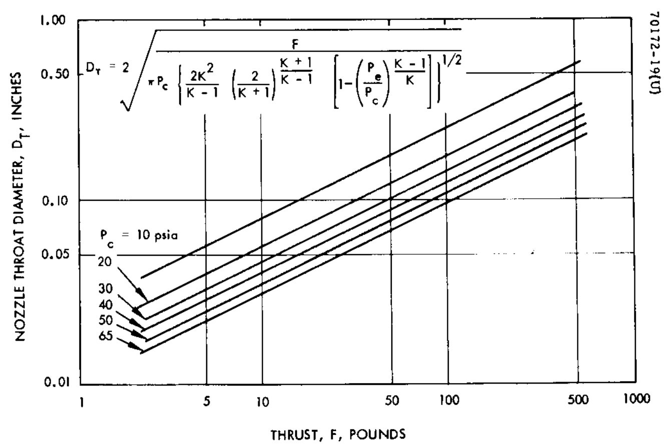


Figure B-3. Nozzle Throat Diameter Versus Thrust for Various Chamber Pressures boundary layer losses, with some reduction in nozzle performance at lower thrust levels. For example, the calculated I_{sp} at the maximum pulsed thrust level considered (0.6 pound) is approximately 75 seconds while at the lower level (0.04 pound), I_{sp} is approximately 74 seconds. This loss was considered insignificant in comparison with the nonrepeatable performance resulting from operating the thruster at very short pulse periods, i.e., when $\Delta T_{pulse} < \Delta T_{valve\ opening}$.

Greer has correlated experimental data with the analytical predictions for pulsed operation (Figure B-4) and has demonstrated, as predicted by the simplified theory, that specific impulse is practically independent of pulse period, particularly for pulses of over 20 milliseconds.* Notable is the slight decrease of specific impulse with decreasing command pulse periods. This effect apparently arises from the valve opening and closing dynamics discussed above. On the basis of Greer's work, a constant specific impulse (I_{sp}) of 73 seconds for pulsed operation was assumed. This value is also substantiated by some unpublished experimental work at Hughes.

It appears that maximum nozzle efficiency is achieved at expansion ratios of approximately 100 to 1 (see Appendix I). Since the expansion nozzle weights except for the retro nozzle are an insignificant part of total system weight, and performance is not appreciably affected over the range from 60 to 120, an expansion ratio of 100 to 1 was used for all nozzle designs. The retro mode thruster size was established by the maximum impulse requirement of 2000 lb_f-sec for a 0.35 hour duration, which will require a 3.33 lb_f thrust level total or thrusters of 1.67 and 0.83 lb_f per nozzle.

PROPELLANT TANK

A design specific impulse of 73 seconds was used to establish required propellant weight and propellant tank size and weight. Calculations of tank shell thickness, volume, and weight were made by the following formulas:

$$V = 4/3\pi r^3$$

$$t = NPr/2S$$

$$A = 4\pi (r + t/2)^2$$

$$W = A\rho t$$

where

$V \equiv$ volume

$r \equiv$ radius

* op. cit.

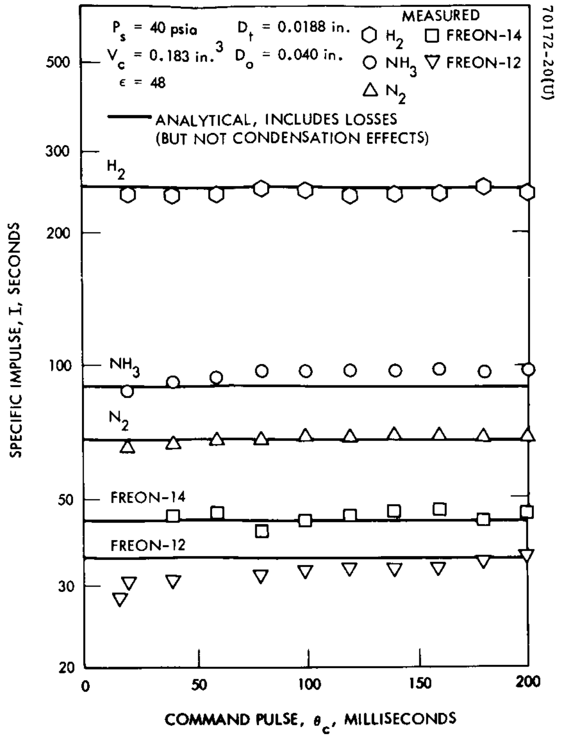


Figure B-4. Specific Impulse, Command Pulse Correlation

(From Greer and Griep)

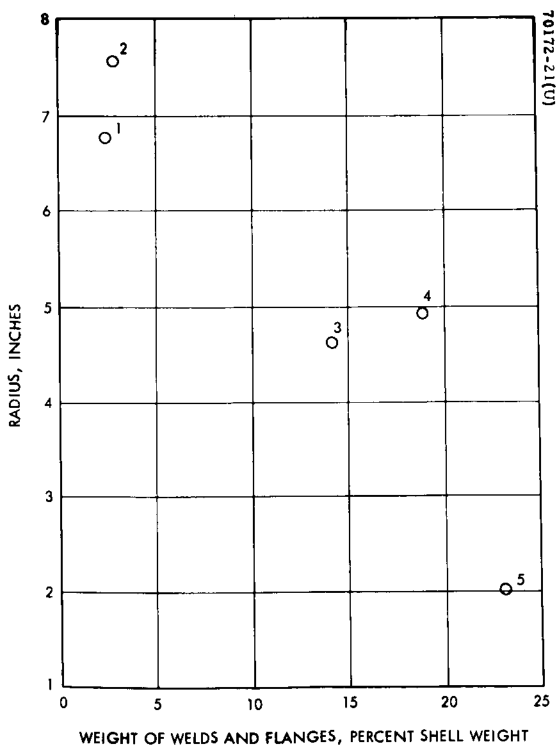


Figure B-5. Surveyor Tank Radius Versus Weight of Welds and Flanges t $\equiv$ thickness

P $\equiv$ operating pressure

N $\equiv$ safety factor (2.2 applied to S)

S $\equiv$ ultimate stress

A $\equiv$ surface area

p $\equiv$ material density

W $\equiv$ tank weight.

The selection of storage pressure is primarily a design packaging choice because tank size varies with storage pressure but tank weight is relatively unaffected over the pressure range considered (3000 to 5000 psi). A nominal 3500 psi pressure was selected.

The actual tank designs include allowance for unexpelled propellant at minimum final pressure and an allowance for tank mounting lugs and connecting line bosses. Because pressure regulator output regulation accuracy is affected by input pressure level, and particularly so as input pressure approaches the output regulated pressure, regulators for 50 psi outlet pressure are generally designed for no less than 200 psi inlet with initial conditions of 3500 psi. The selected regulator is designed for a minimum input pressure of 200 psi, and the allowance for the unused propellant was 5.7 percent, (i.e., 200/3500 x 100 = 5.7 percent). As a practical means for establishing actual tank weight, including bosses and mounting lugs, the percent-above-theoretical weights of several Hughes-developed titanium tanks (shown in Table B-5) were plotted as a function of tank radius (Figure B-5). For large tanks of the size considered (> 8 inches), the increase is approximately 3 percent of the theoretical minimum.

From a review of structural materials literature and discussions with pressurized vessel suppliers, the most efficient tank materials available were bound to be titanium alloy (6 Al-4V), "Ardeformed" stainless steel, and filament-wound epoxy composites. Table B-6 summarizes strength-to-weight ratios of the three materials.

The performance of 6Al-4V titanium alloy is well known and its producibility is well established; because of its longer development history, it represents the lowest risk material choice for nitrogen pressure vessels.

"Ardeformed" vessels are produced by a proprietary development process involving stretch forming AISI3101 type stainless steel at cryogenic temperatures. Considerable testing has been conducted by the developer, Arde-Portland, who claims good corrosion resistance, low notch sensitivity, and good fatigue life. Special forming tooling is required for each design and the lead time would have to be considered in any application.

TABLE B-5. SURVEYOR TANK DATA SUMMARY

Volume, cubic inches	Operating Pressure, psig	Medium	Number of Tanks	Internal Radius, inches	Shell Thickness, inches	Shell Weight, pounds	Average Actual Weight, pounds	Percent Above Theoretical Minimum Weight
1298	5175	He	24	6.77	0.273	26.39	27.04	2.47
1752	5175	He	23	7.49	0.309	36.45	37.39	2.58
398	5000	He	25	4.56	0.184	8.05	9.18	14.05
478	3450	N_2	19	4.87	0.114	5.60	6.66	18.91
33	3181	Gaseous CO_2	10	1.98	0.054	0.440	0.540	22.7

NOTES: 1) All tanks are spherical and fabricated from 6Al-4V titanium.

2) Reference: F. W. Anderson, "Implementation of High Reliability Objectives of Surveyor Pressure Vessels", American Society of Metals Technical Report C6-2.1, 1966.

Ardeformed pressure vessels have been used quite extensively in spacecraft systems; three examples are:

Organization	Program	Application
Lockheed MSC	Agena	Oxygen storage, 10,000 psi
Hamilton Standard/UAC	Apollo Backpack	Oxygen storage
NASA MSC	Gemini C	CO_2 bottle, 5600 psi

Filament wound pressure vessels have been under development for several years; however, successful application in systems has not been extensive. There has been and continues to be considerable interest in filament-wound materials because of the theoretically high strength-to-weight ratios, greater than 2.5 x 10^6 inch.

This high strength-to-weight ratio, which can be demonstrated for simple structures, has not been realized in actual vessel design because, in transforming the material into a practical tank, considerable weight is added by stiffeners and additional material buildup at ports and attachment fittings, by the adapter flanges for ports, and by the additional wall thickness required to achieve acceptable pressure cycling life. When additional material thickness and stiffeners are added to compensate for these practical limitations, a more representative value, obtained by comparing actual filament-wound tank burst strength and tank weights with an equivalent titanium tank, would be the 1,330,000 listed in Table B-6.

TABLE B-6. STRENGTH-TO-WEIGHT RATIOS OF TANK MATERIALS

Material	Yield Strength, psi	Density, lb/in^3	Strength-to-Weight Ratio
Titanium (6Al-4V)	145,000 (1)	0.16	905,000
Ardeformed 301 stainless steel (aged)	300,000 (2)	0.28	1,070,000
Filament-wound epoxy composite	200,000 (3)	0.075	2,670,000, 1,330,000 (4)

NOTES: 1) MIL Handbook 5.

2) "High Performance Pressure Vessels", Report P-30715E, Arde-Portland, Inc., Paramus, N. J., May 1964.

3) "Filament Wound Pressure Vessels", Advanced Structures Division, Whittaker Corporation, La Mesa, California.

4) Although the manufacturers' literature on filament wound structures would indicate a 2,670,000 strength-to-weight ratio from yield strength and material density, if the test burst strength and weight of actual off-the-shelf tanks are used the 1,330,000 psi ratio is more useful as a direct comparison with metallic tanks.

Filament wound vessels usually require elastomeric or metallic film liners because the basic epoxy shell is very permeable to most gases. With liners, leak rates of less than 0.05 $cc/hr/in^2$ of internal surface area can be achieved. Water absorption in the epoxy has also created problems because of vapor expansion of the water at high altitude pressures, resulting in small fissures, and in some cases, with the combined effect of pressure cycling, in actual tank failures. Military standards have been established for filament wound vessels (MIL-T-2536B) and at least one manufacturer, Advanced Structures Division of Whittaker Corporation, has met these military standards; the General Dynamics F-111 aircraft presently employs such tanks. However, the manufacturing variations of filament tanks still has prevented the widespread use of such vessels, and further development is required before they are considered state-of-the-art.

From the above considerations, cryogenically formed ("Ardeformed") stainless steel tanks are proposed as a state-of-the-art baseline design material for the nitrogen system. If the proposed application is several years away, however, the technology of filament-wound tanks should be re-evaluated because the reliability might be sufficiently improved to make the weight savings attractive.

The nitrogen system baseline design summary, Table B-2, includes Ardeformed tanks, using a 2.2 times ultimate strength safety factor, and a 3 percent allowance for mounting lugs and bosses.

APPENDIX C. SUPPORTING ANALYSES – HYDRAZINE CATALYTIC PLENUM SYSTEM

Two general approaches may be taken to the design of a hydrazine catalytic decomposition plenum: a high efficiency insulated system that makes use of a large fraction of the thermal energy released in the decomposition reaction, or a larger system made more reliable by dissipation of heat before the gas reaches a valve. A reliability assessment indicates a preference for low gas temperature, primarily because of the lower reliability of hot gas valves.

If only the attitude control function were required, no option would exist. The control pulses are so small and infrequent that the propellant gas downstream of a 10-foot line would be at ambient temperature regardless of the system design. The gas generator would be designed for maximum ammonia dissociation to obtain better I_{sp} and avoid liquid condensation in the lines, and the lines themselves would be placed in good thermal contact with the solar panels or positioned to receive solar illumination.

If hot gas valves were tolerable, the steady-state operation required for maneuvering would offer an opportunity for a performance improvement over ambient propellant gas. With a properly designed catalyst bed and well-insulated lines, it would be possible, in principle, to approach the 230 + lbs-sec/lb_m attained by conventional catalytic engines. In practice, however, an insulated line could not be used unless heaters were employed to keep the line at a reasonable temperature during off periods. One alternative would be to expose the feed line to solar radiation, as stated above. Note that the line probably cannot be tied to the solar panel because of high heat flux to the solar cells. A rather optimistic estimate indicates that temperature might be maintained in the 400 to 1000°F range, with maximum specific impulse in the vicinity of 200 lb_f-sec/lb_m.

Despite the potential gain in performance, a hot gas system is not recommended for two important reasons. First, no valve is available that can operate reliably at more than a few hundred degrees and has good tolerance for the particulate contamination resulting from catalyst attrition. Also, filter requirements are difficult for very hot gas. Secondly, for extended steady-state operation, higher performance can be obtained from individual conventional liquid hydrazine monopropellant thrusters with little, if any, increase in system weight and less risk than the hot gas system with a lesser number of catalyst beds (perhaps only one).

A detailed list of components and weights is shown in Table C-1.

TABLE C-1. HYDRAZINE CATALYTIC PLENUM COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Component	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Fixed weight															
Fill valve	1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(4) 5.2	(4) 5.2	(4) 5.2	(4) 5.2	(4) 5.2	(4) 5.2	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Liquid control valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Gas control valve															
Cruise	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Retro	1.4	— —	— —	(16) 22.4	(16) 22.4	— —	— —	(16) 22.4	(16) 22.4	— —	(4) 5.6			(8) 11.2	(8) 11.2
Pressure switch	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.50	(2) 0.50	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Pressure transducer	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.50	(2) 0.50	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Filter	1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(4) 6.4	(4) 6.4	(4) 6.4	(4) 6.4	(4) 6.4	(4) 6.4	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2
Nozzle thrust															
0.04	0.06	(12) 0.72	— —	(12) 0.72	— —	(12) 0.72	— —	(12) 0.72	— —	(12) 0.72	(12) 0.72	(6) 0.4		(6) 0.4	
0.08	0.06	— —	(24) 1.44	— —	(24) 1.44	— —	(24) 1.44	— —	(24) 1.44	— —	— —		(12) 0.7		(12) 0.7
0.83	0.09	— —	— —	— —	(8) 0.72	— —	— —	— —	(8) 0.72	— —	— —				(4) 0.4
1.67	1.3	— —	— —	(4) 0.52	— —	— —	— —	(4) 0.52	— —	— —	(4) 0.52			(2) 0.4	
Gas generator	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Manifolds and bracketry		2.31	2.31	2.31	2.31	2.31	2.31	2.31	2.31	2.31	2.31	(2) 2.31	(2) 2.31	(2) 2.31	(2) 2.31
Total fixed weights		31.5	32.2	54.4	55.3	40.8	41.5	63.7	64.6	26.4	32.5	21.6	21.9	33.2	33.5
Propellant		28.1	28.1	40.2	40.2	56.2	56.2	80.4	80.4	84.3	20.6	28.1	28.1	40.2	40.2
Tank plus bladder		1.52	1.52	2.18	2.18	3.04	3.04	4.36	4.36	4.56	6.54	1.52	1.52	2.18	2.18
Plenum chamber		3.94	3.94	3.94	3.94	7.88	7.88	7.88	7.88	7.88	7.88	3.94	3.94	3.94	3.94
Nitrogen pressurant		0.56	0.56	0.81	0.81	1.12	1.12	1.62	1.62	1.68	2.43	0.56	0.56	0.81	0.81
Total		65.6	66.3	101.5	102.5	109.0	109.8	158.0	158.9	124.8	170.0	55.7	56.0	80.3	80.6

THRUSTER DESIGN

Both the nitrogen and hydrazine plenum systems must provide the same degree of spacecraft control. Because of the general similarity to the nitrogen system in flow parameters, an expansion ratio of 100 was also used for the hydrazine plenum system.

The effects of thrust and chamber pressure on throat diameter were determined; the results are shown in Figure C-1. It is seen that an operating pressure of 50 psi is satisfactory for this application. The results of a study of variations in NH_3 dissociation on nozzle size are shown in Figure C-2; for a selected throat diameter (thruster), a 100 percent variation in NH_3 dissociation would cause a 10 percent variation in thrust level. The catalyst bed degradation rate will cause variations in the degree of NH_3 dissociation throughout the mission, although the bed may be designed to minimize this effect. More experimental information is needed in this area.

THRUSTER VALVE DESIGN

As noted previously, fast opening and closing times are necessary to meet the repeatable impulse bit requirement. The size of the valve (effective flow areas) also has an effect on the impulse bit; i. e., a rapid thrust buildup requires that the rate of flow into the thrust chamber be much greater than the rate of flow out through the nozzle throat. It has been shown that this rate is a function of the relative flow areas (Ao/At) and the specific heat ratio of the gas.* A relationship has been derived in terms of a parameter "B", which has been experimentally correlated with chamber pressure rise rates. The equation and results are shown in Figure C-3. Suggested design values lie between 5 and 10. Selection of the value of "B" (i. e., the valve size) requires additional information on high temperature valves.

To determine the minimum allowable system temperature, the possibility of ammonia condensation upstream of the thruster valve was investigated. In the event of gas cooling during nonfiring periods, any undissociated NH_3 may condense in the line. This could result in unstable two-phase flow during thruster operation with associated thrust instabilities. Since the percent concentration of NH_3 is known for any degree of dissociation, the relative vapor pressure may be determined. The temperature at which this ammonia will begin to condense may be found in Figure C-4 and places a limit on the low temperature of the system. (The exception, of course, is the case of complete NH_3 dissociation, which is not temperature-limited by NH_3 condensation.) Results are presented in Figure C-5. It should be emphasized that these results do not determine the valve or system temperature, only the lower limits. Influence of ammonia dissociation on performance is shown in Figure C-6.

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*H. Greer and D. J. Griep, "Dynamic Performance of Low Thrust Cold Gas Reaction Jets in a Vacuum," SSD-TR-66-180, August 1966. See discussion in Appendix B.

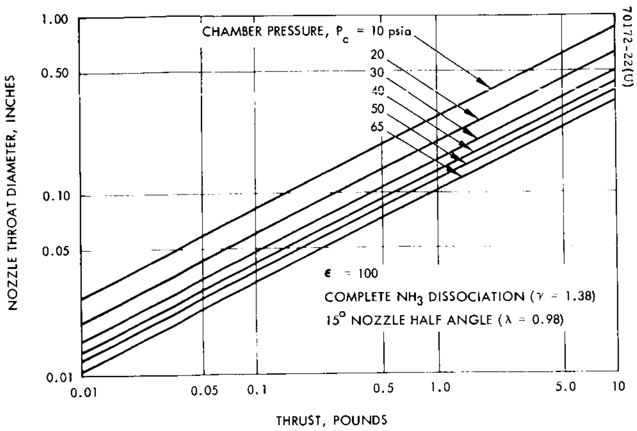


Figure C-1. Nozzle Throat Diameter Versus Thrust and Chamber Pressure

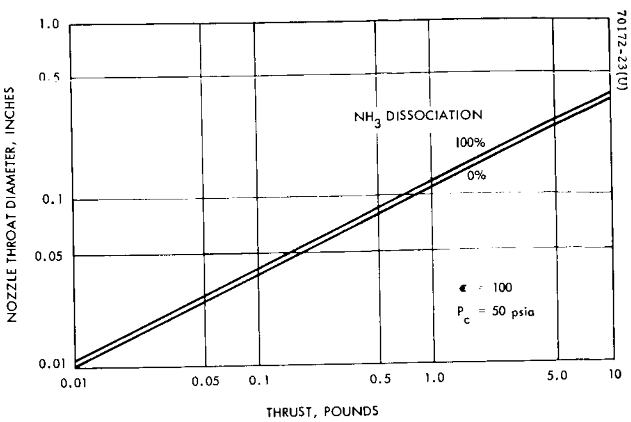


Figure C-2. Effect of Variations in NH3 Dissociation on Engine Thrust

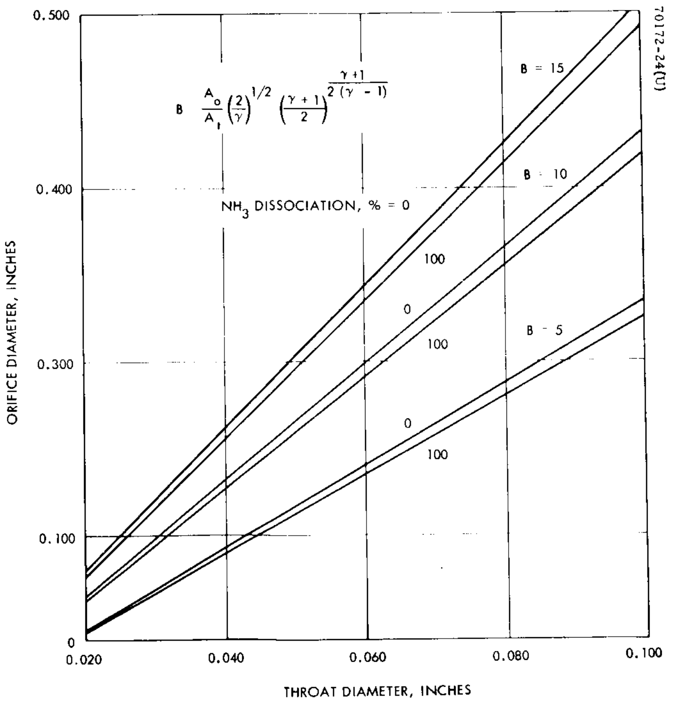


Figure C-3. Effect of Response Parameter "B" on Nozzle Inlet Orifice Size

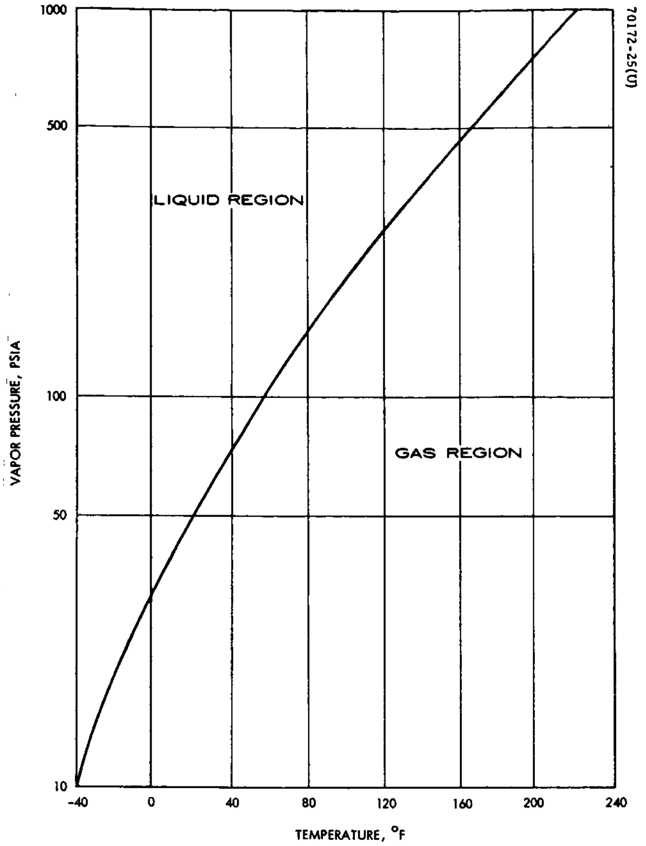


Figure C-4. Ammonia Vapor Pressure Versus Temperature

(Data from Handbook of Chemistry and Physics, 32nd Edition, Chemical Rubber Publishing Co., Cleveland, Ohio)

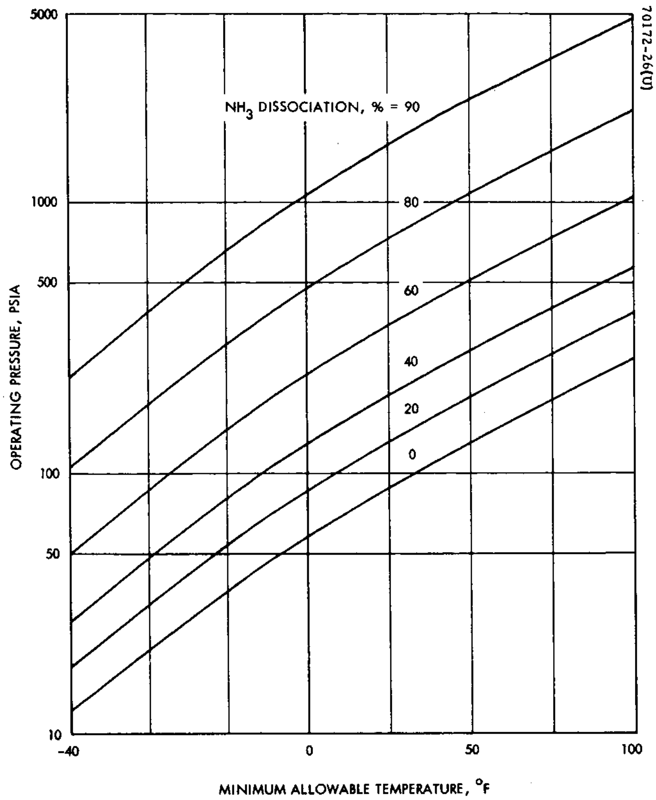


Figure C-5. Minimum Soak Temperatures to Preclude NH_3 Condensation

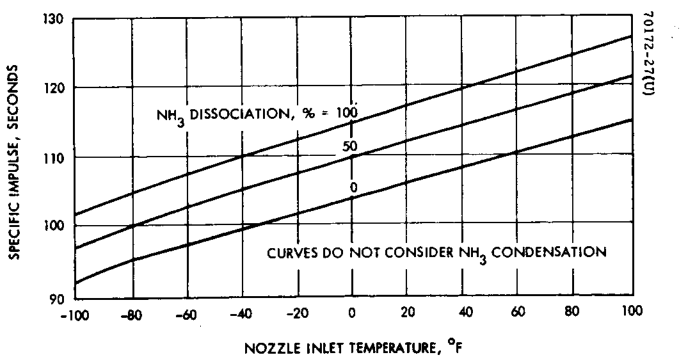
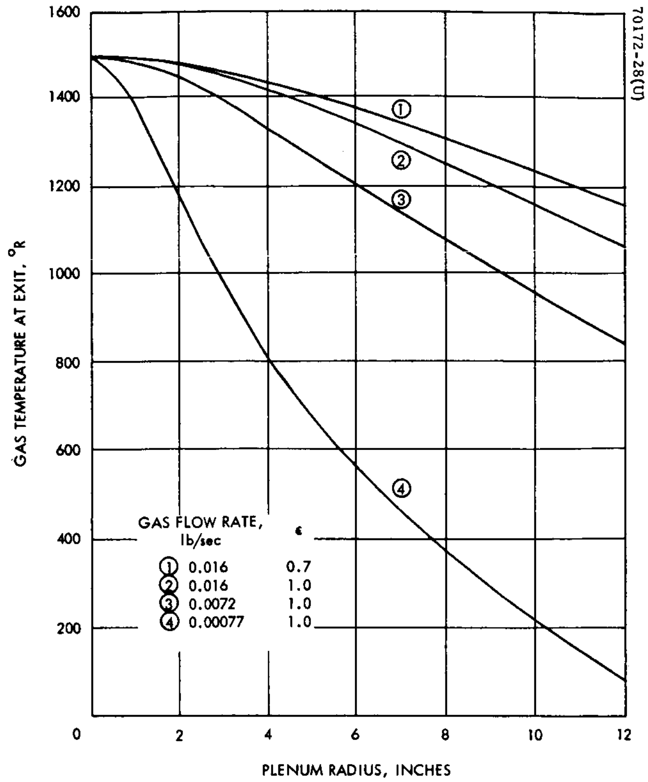


Figure C-6. Effect of Gas Cooling on Plenum System Performance



1. SOLAR HEAT INPUT = 0
2. 50% OF PLENUM SURFACE EXPOSED TO DEEP SPACE
3. GAS/PLENUM HEAT TRANSFER COEFFICIENT ⇒ ∞
4. 100% NH_3 DISSOCIATION
5. STEADY STATE CONDITIONS
6. GAS INLET TEMPERATURE = 1500°R
7. RESULTING EQUATION:

$$\dot{\omega}_g C_g (T_{g_i} - T_{g_o}) = \epsilon A_s \sigma \left[\frac{1}{2}(T_{g_i} + T_{g_o})\right]^4$$

Figure C-7. Gas Cooling in Plenum Versus Radius and Gas Flow Rate

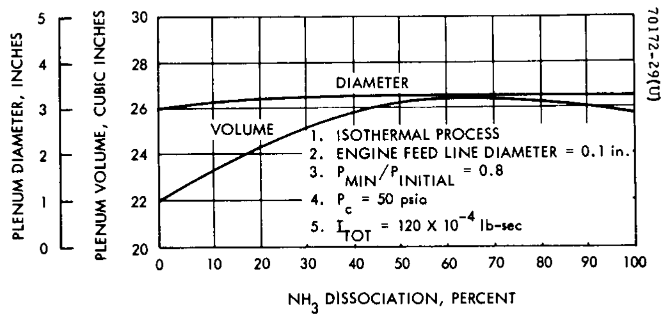


Figure C-8. Plenum Size for Pulse Mode Operation

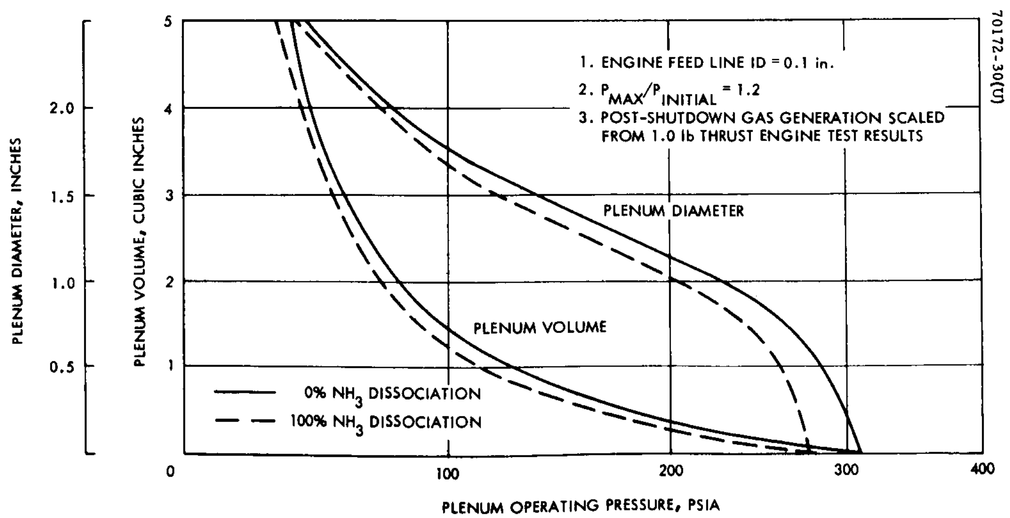


Figure C-9. Plenum Size to Limit Pressure Overshoot at Gas Generator Cutoff

Small amounts of water originally present in the propellant and unreacted hydrazine will also exist in the gas distribution system. Experimental determination of the amounts present is required to evaluate the severity of water and hydrazine condensation. It is thought, however, that thermal conditioning of the distribution system to a temperature above the dew point, or at least the freezing point of hydrazine, would be most desirable. See the discussion on thermal conditioning in Appendix H.

PLENUM DESIGN

A preliminary analysis has been made for cooling within variously sized plenums. In order to set a bound on available cooling, it was assumed that gas from a generator having 100 percent ammonia dissociation (i.e., the coolest composition) could be introduced with sufficient turbulence to accomplish rapid and complete mixing. The heat transfer coefficient from gas to plenum wall was assumed infinite. Curves 2, 3, and 4 in Figure C-7 show the predicted gas exit temperatures for flow rates between full retro roll control and attitude pulse operation when plenum emissivity is 1.0. A more realistic condition is curve 1, for which plenum emissivity was 0.7. In all cases the plenum was assumed half radiating to space and half insulated. Details of the calculations are given at the end of the Appendix.

It is seen that flow rate may have a very substantial effect on gas exit temperature and, at high flow rates, a very large plenum would be required for large temperature drops. Consequently, the aforementioned feedline analysis is important for the present calculations. It is thought that the results of Figure C-7 are at least reasonable. JPL reports that an existing experimental plenum 15 inches in diameter, when operated at a flow rate of 2×10^{-3} lb/sec, gave a plenum temperature of about 170°F after 30 seconds continuous firing. If the convective cooling of the laboratory conditions were comparable with the calculated radiation cooling conditions, Figure C-7 would predict similar temperatures.

The object of all these calculations was to establish reasonable bounds on achievable temperature so that minor adjustment of thermal management could, in fact, produce the required conditions. It is, however, evident that an experimental program is absolutely necessary to produce optimum functional hardware.

The plenum chamber is called upon to provide two functions during system operation:

1) Maintain thruster supply pressure at an acceptable level during pulse mode operation.

2) Limit system pressure rise after the liquid hydrazine feed valve closes.

Analysis of these two functions (detailed later) resulted in Figures C-8 and C-9. The figures show that plenum size is determined by the pulse mode condition which, as shown in Figure C-8, is not strongly dependent on the degree of NH_3 dissociation.

It is seen that the above requirements are satisfied by a plenum of only a few inches in diameter. However, this is a practical design only if components and materials of construction are available that will allow generation of gas on demand at or near the hydrazine adiabatic decomposition temperature. It is improbable that a suitable valve or filter will be found for operation near 1800°F. Consequently, it is likely that gas temperature will have to be reduced to a tolerable level by reradiation of heat from the line, storage of a large quantity of cool gas in the plenum, or both. The large thrust increments required during commanded turns, midcourse, and retro maneuvers would require a large increase in plenum size and weight in order to limit the maximum valve temperature to a few hundred degrees. Analysis of such designs could be undertaken after the tolerable temperature range is better defined.

THRUSTER FEED LINE DESIGN

A steady-state heat transfer analysis of the thruster feed line at high (retro phase) flow rates shows negligible heat transfer from the gas to the surroundings. Results are shown in Figure C-10.

With the above information (i. e., negligible heat transfer) the pressure drop in the line was recalculated at high flowrate using "Fanno Flow" thermodynamics. These results are shown in Figure C-11. It is seen that the pressure losses are acceptable for this size line, e. g., a plenum pressure of 75 psia will allowgreater valve loss of 12 psi.

A steady-state heat transfer analysis of the plenum chamber has also shown negligible heat transfer at high flow rates. Consequently an adiabatic transient analysis of the plenum temperature and pressure history was performed. The results, shown in Figure C-12, indicate that the gas outlet temperature rises rapidly even in large plenums which start at static equilibrium conditions (522°F).

Thus, for the "on" times under consideration, the use of a large plenum will not significantly reduce gas temperatures at the thruster feedline outlet. Accompanying the temperature variation in the plenum is a transient pressure fluctuation (Figure C-12a). This pressure surge of approximately 12 psi is within the ±20 percent variation specified.

The transient temperature history of the thruster feedline and the corresponding gas temperature at the inlet to the thruster were computed using the transient results obtained for a 24-inch diameter plenum (Figure C-12). The results are shown in Figure C-13 and show that, although the line temperature initially lags the gas temperature, within 200 seconds they are very close. This means that components (valves and filters) capable of withstanding high temperatures must be used for these flow conditions.

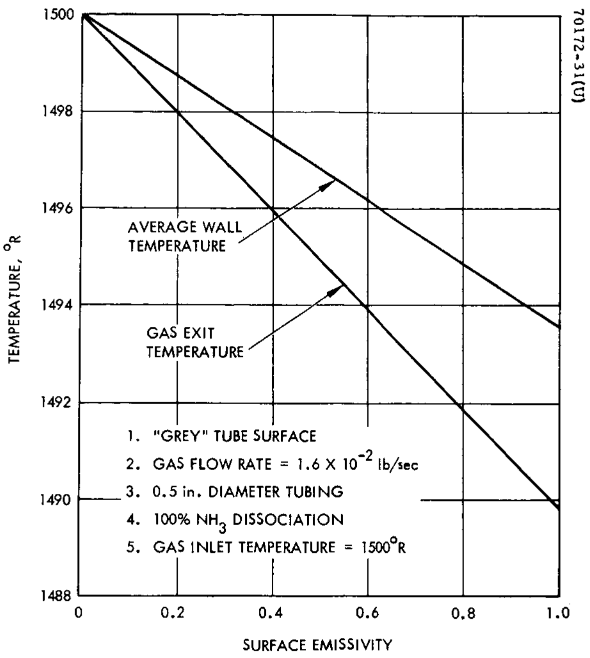


Figure C-10. Steady State Heat Transfer from Nozzle Feed Line (High Flow Rates)

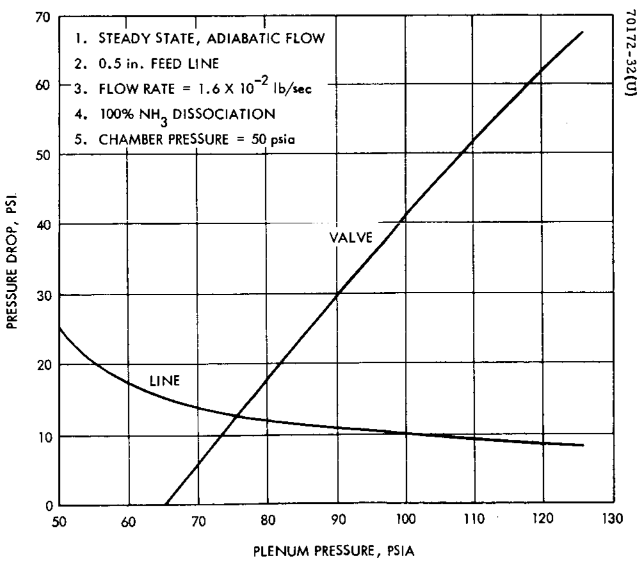
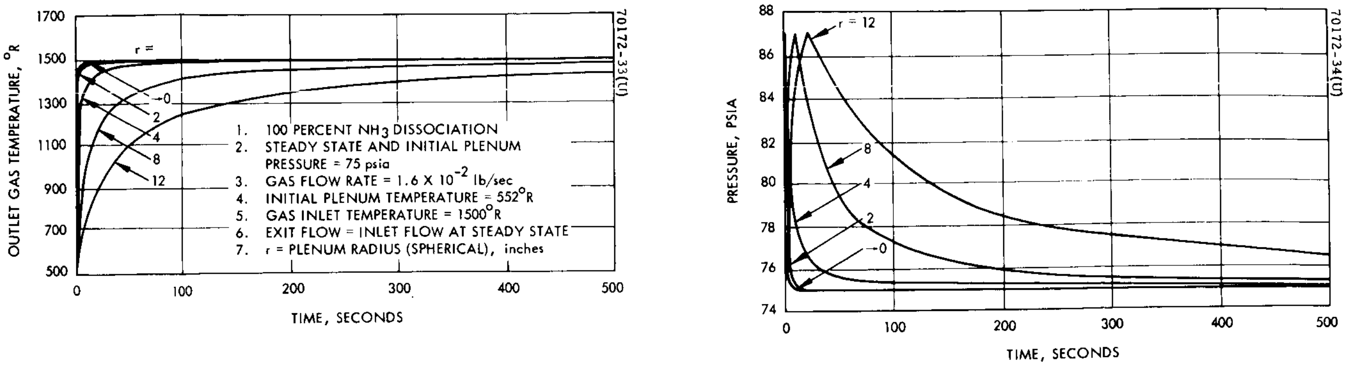


Figure C-11. Thruster Feed System Pressure Loss



a) Transient Response

b) Pressure History

Figure C-12. Adiabatic Plenum Transient Response and Pressure History Versus Plenum Size at High Flow Rate

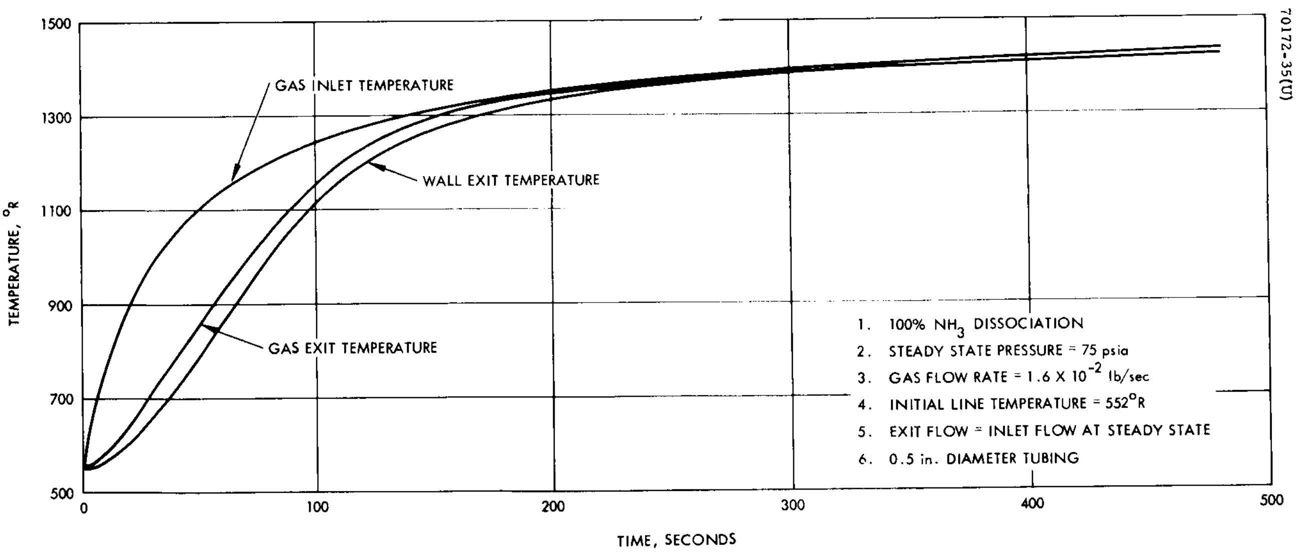
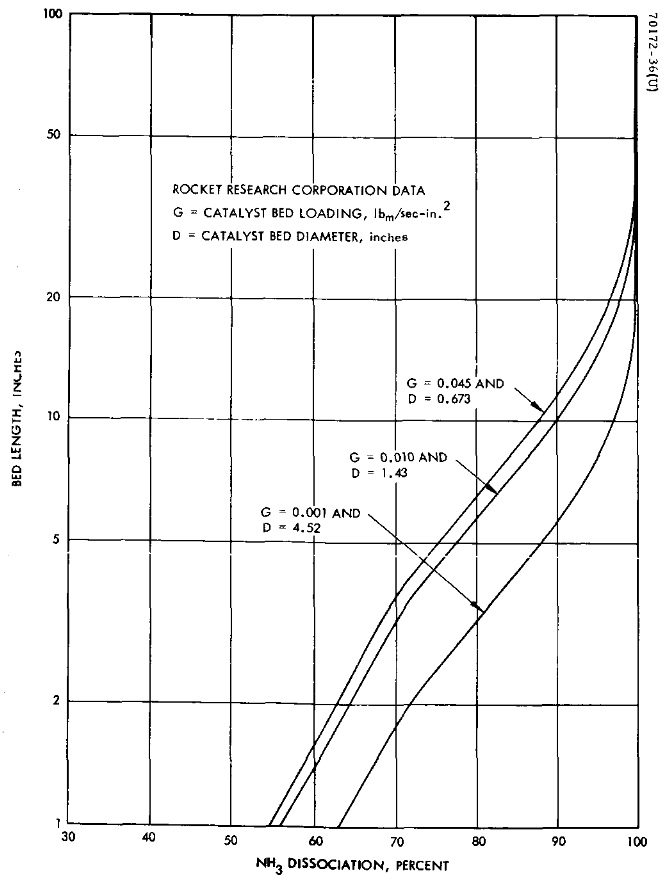


Figure C-13. Adiabatic Line Transient Response at High Flow Rate

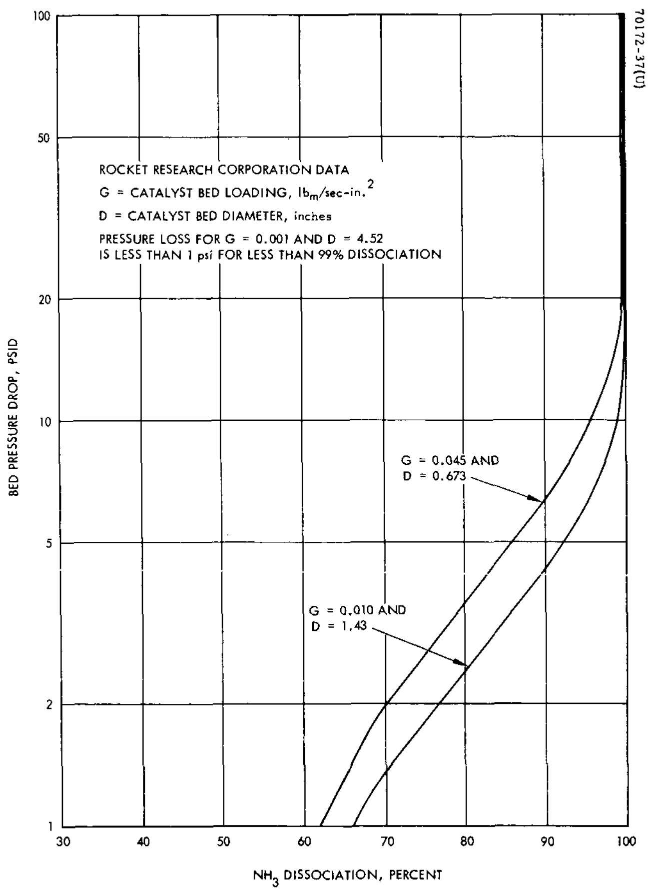
It is concluded that without exotic heat transfer devices the system produces high-exit temperatures at high flow rates and cannot be recommended for these requirements; however, the gas cooling available at low flow rates is adequate to allow use of state-of-the-art component designs.

CATALYST BED DESIGN

An analysis of the catalyst bed size was also performed using data supplied by Rocket Research Corporation. The results are shown in Figure C-14. Figure C-14a shows that bed sizes become excessive at high percentages of ammonia dissociation; 80 percent dissociation may be the practical upper limit. In this event, somewhat higher temperatures than those shown in the previous figures must be tolerated. Figure C-14b indicates that pressure drops through the beds are nominal, and small compared with the bed injector pressure drop (50 psi).



a) Bed Size



b) Bed Pressure Drop

Figure C-14. Catalyst Bed Characteristics Versus Percent of NH_3 Dissociation

DERIVATION OF EQUATIONS USED TO PRODUCE FIGURES C-1 THROUGH C-13

Figures C-1 through C-6 are either adequately explained in the text or have obvious governing equations. The remainder of the figures are explained in the following paragraphs.

Figure C-7 – Gas Cooling in Plenum Versus Radius and Gas Flow Rate

A steady-state heat balance on the plenum wall gives (Reference 1):

$$q(\text{in-gas}) + q(\text{in-radiation}) = q(\text{out-radiation}) \tag{1}$$

where

$$q(\text{in-gas}) = hA_i(\overline{T}_g - \overline{T}_w) \tag{2}$$

$$q(\text{in-radiation}) = a_S F_S G_S A_S \tag{3}$$

and

$$q(\text{out-radiation}) = \epsilon \sigma A_S \overline{T}_w^{\,4} \tag{4}$$

To promote gas cooling and therefore component life, baffles or fins are assumed to be used to maximize the gas to plenum heat transfer coefficient. It follows then that the wall temperature would be close to the average gas temperature and

$$\overline{T}_g = \overline{T}_w \text{ where } T_g = \frac{T_{g1} + T_{g2}}{2} \tag{5}$$

The energy given up by the gas in flowing through the plenum is

$$q(\text{in-gas}) = \dot{W}_g C_g (T_{g1} - T_{g2}) \tag{6}$$

If the plenum views the sun, F_S is 0. 5; if not, F_S is 0. In the present case, the radiation heat input from the sun has been found to have a negligible effect on system operating temperature. Only the nonoperating temperature and, hence, the initial temperature for the transient studies are affected.

Combining these assumptions with Equations 1, 3, 4, 5, and 6

$$\dot{W}_g C_g (T_{g1} - T_{g2}) = \epsilon A_S \sigma \frac{1}{2} \left[(T_{g1} + T_{g2}) \right]^4 \tag{7}$$

This equation was solved by iteration for T_{g1} over the range of independent variables shown in Figure C-7.

Figure C-8 – Plenum Size for Pulse Mode Operation

The minimum plenum size is that which can deliver at least one pulse without violating the allowable thrust variation. Since thrust is directly proportional to pressure, the pressure at pulse termination must be at least 80 percent of the initial value, i.e.,

$$P_{min}/P_{initial} = 0.80 \tag{8}$$

This limit will result in a very small temperature change within the system. Consequently, an isothermal process assumption is valid.

The initial plenum gas weight is given by

$$W_{g1} = \frac{P_{g1} V}{RT} \tag{9}$$

and the final gas weight is

$$W_{g2} = \frac{P_{g2} V}{RT} \tag{10}$$

where

$$\Delta W_g - W_{g1} - W_{g2} = \frac{I_{TOT}}{I_{SP}} \tag{11}$$

and

$$V = V_{plenum} + V_{line} \tag{12}$$

Solving Equations 9, 10, 11, and 12 simultaneously,

$$V_{plenum} = \frac{I_{TOT} RT}{P_{g2} I_{SP}} \left(\frac{P_{g2}/P_{g1}}{1 - P_{g2}/P_{g1}} \right) \tag{13}$$

where T is established by the percent of ammonia dissociation and the other independent variables listed in Figure C-8.

In a practical system, heat transfer very likely will result in a plenum gas temperature lower than the adiabatic flame temperature. In this event the minimum size plenum will be even smaller than that shown in Figure C-8, since gas volume is directly proportional to temperature while specific impulse decreases as the square root of temperature.

<u>Figure C-9 — Plenum Size to Limit Pressure Overshoot at Gas Generator Cutoff</u>

This is very similar to the preceding analysis, except for the independent parameters listed in the figure. The actual amount of gas generator overshoot will be a function of the particular generator selected and is best determined by experiment. The calculations shown here were based on such test data and are thought to be typical of a wide variety of generators. However, since the plenum contributes a minor portion of the total system weight, a 2-foot diameter plenum was selected for the final design in order to provide a large margin of safety.

<u>Figure C-10 — Steady-State Heat Transfer From Nozzle Feed Line (High Flow Rates)</u>

Performing a heat balance on the line:

Heat flux from flowing gas + heat flux from solar input = heat flux radiated to space or

$$h\Delta A_i(\overline{T}_g - \overline{T}_w) + A_S F_S G_S \Delta A_{ou} = \epsilon\sigma \overline{T}_w^{\,4} \Delta A_{ou} \tag{14}$$

where the gas and wall temperatures may be related by a heat balance on the fluid, i. e., the heat flux from the gas equals the flux into the wall; or

$$Q_g \text{ in} - Q_g \text{ out} = h\Delta A_i(\overline{T}_g - \overline{T}_w)$$

or

$$\dot{W}_g C_g T_{g1} = \dot{W}_g C_g \overline{T}_{g2} = h\Delta A_i(T_g - T_w) + \dot{W}_g C_g (T_{g1} - T_{g2}) \tag{15}$$

where $\overline{T}_g$ is assumed to be

$$T_g = \frac{T_{g1} + T_{g2}}{2} \tag{16}$$

which is a good estimate for short lines but approximate for a long line. Substituting Equation 15 into Equation 14,

$$\dot{W}_g C_g (T_{g1} - T_{g2}) + A_S F_S G_S \Delta A_{ou} = \epsilon \sigma \overline{T}_w{}^4 \Delta A_{ou} \qquad (17)$$

Solving Equation 15 for T_w,

$$\overline{T}_w = \overline{T}_g - \frac{\dot{W}_g C_g}{h \Delta A_i} (T_{g1} - T_{g2})$$

$$= 1/2 \left[T_{g1} \left(1 - \frac{2 \dot{W}_g C_g}{h \Delta A_i} \right) + T_{g2} \left(1 + \frac{2 \dot{W}_g C_g}{h \Delta A_i} \right) \right] \qquad (18)$$

and substituting into Equation 17,

$$\dot{W}_g C_g (T_{g1} - T_{g2}) + A_S F_S G_S \Delta A_{ou} = \epsilon \sigma A_{ou} \left\{ 1/2 \left[T_{g1} \left(1 - \frac{2 \dot{W}_g C_g}{h \Delta A_i} \right) + T_{g2} \left(1 + \frac{2 \dot{W}_g C_g}{h \Delta A_i} \right) \right] \right\}^4 \qquad (19)$$

The only unknown in the above equation is T_{g2}, the gas outlet temperature; however, because of the form of the equation, it must be solved either by iteration or graphically. To get an estimate of the heat transfer from a long line, Equation 19 may be put in the form

$$\dot{W}_g C_g (T_{g1} - T_{g2}) + A_S F_S G_S A_{ou} = \epsilon \sigma A_{ou} \left\{ 1/2 \left[T_{g1} \left(1 - \frac{2 \dot{W}_g C_g}{h \Delta A_i} \right) + T_{g2} \left(1 + \frac{2 \dot{W}_g C_g}{h \Delta A_i} \right) \right] \right\}^4 \qquad (20)$$

The heat transfer coefficient was found from the Reynolds analogy

$$\frac{f}{2} = \frac{h_{in} A_i}{C_g W_g} (Pr)^{2/3} \qquad (21)$$

where region of analogy validity and friction factor were determined from the appropriate Reynolds number (Reference 2).

Figure C-11 – Thruster Feed System Pressure Loss

The procedure used to generate this curve (Reference 3) is:

1) Assume steady-state plenum pressure.

2) Calculate gas properties at inlet to line.

3) Compute line inlet Mach number.

4) Find exit Mach number and corresponding pressure from "Fanno" thermodynamic tables.

5) Determine the line pressure drop as the difference in pressure between line inlet and exit.

6) Determine the difference between line exit pressure and 50 psia (the desired thruster chamber pressure), i. e. , the allowable valve pressure drop.

Figure C-12 – Adiabatic Plenum Transient Response and Pressure History Versus Plenum Size at High Flow Rate

The plenum chamber gas is assumed to be at equilibrium with the plenum at a temperature of 552°F. The gas generator is activated and the incoming hot gas mixes uniformly and completely with the initial gas. The appropriate temperature history is derived from a mass balance:

$$\begin{aligned} \text{Energy at any time } (E(t)) &= \text{Initial energy } (E(O)) \\ &+ \text{Energy In } (E(1)) \\ &- \text{Energy Out } (E(2)) \qquad (22) \end{aligned}$$

where

$$E(t) = W_{gtotal}(t) C_g \overline{T}_g \tag{23}$$

$$E(0) = W_{g0} C_g T_{g0} \tag{24}$$

$$E(1) = \dot{W}_{g1} C_g T_{g1} \Delta t \tag{25}$$

$$E(2) = \dot{W}_{g2} C_g T_{g2} \Delta t \tag{26}$$

where for complete mixing

$$T_{g2} = \overline{T}_g \tag{27}$$

Combining Equations 22 through 27 and solving for T_{g2} (C_g = constant):

$$T_{g2} = \frac{W_{g0} T_{g0} + W_{g1} T_{g1} \Delta t}{W_{g0} + \dot{W}_{g1} \Delta t} \tag{28}$$

This equation is parameterized in Figure C-12a with the independent variables listed thereon. The plenum size determines the initial gas weight, W_{go}.

The corresponding pressure history is determined from

$$P_t = \frac{(W_{g0} + \dot{W}_{g1} \Delta t - \dot{W}_{g2} \Delta t) R_{gt} T_{g2}}{V_p} \tag{29}$$

where the exit flow, $\dot{W}_{g2}$, is found from

$$\dot{W}_{g2} = \dot{W}_{2SS} \frac{P_t}{PtSS} \sqrt{\frac{T_{g2SS}}{T_{g2}}} \tag{30}$$

where SS subscript denotes steady-state conditions. The above equation compensates for variations in thruster flow rate as pressure and temperature change. The final equation is

$$P_t = \frac{W_{go} + \dot{W}_{g1} \Delta t}{\frac{V_p}{R_g T_{g0}} + \dot{W}_{g2SS} \left(\frac{1}{P_{tSS}} \sqrt{\frac{T_{0SS}}{T_{g0}}} \right) \Delta t} \tag{31}$$

This equation is parameterized in Figure C-12b for the same conditions as listed in Figure C-12a.

Figure C-13 — Adiabatic Line Transient Response at High Flow Rate

The general transient heat balance on the line is:

> Heat flux from gas + radiation heat input from sun - radiation loss to deep space - conduction loss to supports and connecting components = heat storage in line

The previous analyses have shown that radiation has a negligible effect at steady-state conditions (i. e., high operating temperatures); consequently it will have even less influence at the lower transient temperatures and will be neglected. In addition, it is assumed that the line is well insulated from the supports, again producing negligible heat loss. The governing equations are then:

Heat flux from gas = heat storage in the line,

or

$$hA_i (\overline{T}_g - \overline{T}_w) = W_w C_w \left(\frac{T_{wt} - T_{wo}}{\Delta t} \right) \tag{32}$$

A double averaging technique was used in which the line temperature is averaged over both the segment length and a time interval, Δt. This technique is exact for small line segments and short time intervals and approximate for other conditions. Then:

$$\overline{T}_g = 1/4 \, (T_{g10} + T_{g20} + T_{g1t} + T_{g2t}) \tag{33}$$

$$\overline{T}_w = 1/4 \, (T_{10} + T_{w20} + T_{w1t} + T_{w2t}) \tag{34}$$

However, for infinite segment material conductivity (i. e., high relative to gas):

$$T_{w10} = T_{w20} \text{ and } T_{w1t} = T_{w2t}$$

or

$$\overline{T}_w = 1/2\,(T_{w0} + T_{wt}) \tag{35}$$

The above set of equations contains two unknowns, T_{wt} and T_{g2t}. A third equation is therefore required:

$$hA_i\,(\overline{T}_g - \overline{T}_w) = \dot{W}_g C_g (\overline{T}_{g1} - \overline{T}_{g2}) \tag{36}$$

Using appropriate nomenclature,

$$\overline{T}_{g1} = \frac{T_{g10} + T_{g1t}}{2} \tag{37}$$

and

$$T_{g2} = \frac{T_{g20} + T_{g2t}}{2} \tag{38}$$

Solving Equations 32 through 38 for T_{g2t},

$$T_{g2t} = \frac{(T_{g10} + T_{g1t})\left[1 - \frac{W_w C_w}{W_g C_g \Delta t}\left(1 - \frac{2\dot{W}_g C_g}{hA}\right)\right] + \frac{4W_w C_w T_{w0}}{\dot{W}_g C_g \Delta t}}{\left[1 + \frac{W_w C_w}{\dot{W}_g C_g \Delta t}\left(1 + \frac{2\dot{W}_g C_g}{hA}\right)\right]} - T_{g20} \tag{39}$$

where the segment inlet temperatures, T_{g1}, are equal to the plenum outlet temperatures previously computed, and the initial wall temperature is found from Equation 40 which may be obtained by rearrangement and substitution of Equation 36:

$$T_{wt} = 1/2\left[(T_{g10} + T_{g1t})\left(1 - \frac{2\dot{W}_g C_g}{hA}\right) + T_{g20} + T_{g2t}\left(1 + \frac{2\dot{W}_g C_g}{hA}\right)\right] \tag{40}$$

The above equations may be simplified, as appropriate, by the substitution of Reynolds analogy, as explained earlier. The results for a 12-inch radius diameter plenum are shown in Figure C-13. The line was divided into five segments with the gas outlet conditions of one segment being the inlet conditions for the next. This procedure was satisfactory for the purposes of this study; however, in the event detailed design studies are required, the above equations should be programmed so that small segments and time intervals may be conveniently handled.

Figure C-14 – Catalyst Bed Characteristics Versus Percent of NH_3 Dissociation

These curves were derived directly from data presented by Rocket Research Corporation (Reference 4).

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2. R. B. Bird, W. E. Steward, E. N. Tightfoot, "Transport Phenomena," John Wiley & Sons, Inc., New York, 1962.

3. M. J. Zucrow, "Aircraft and Missile Propulsion," John Wiley & Sons, Inc., New York, 1958.

4. "Monopropellant Hydrazine Design Data," Rocket Research Corporation, Seattle, Washington, circa 1966.

NOMENCLATURE

A = area

a_s = solar absorptivity

C = heat capacity

d = diameter

f = friction factor

F_S = solar view factor

G_S = solar flux

h = heat transfer coefficient

$h\ell n$ = logarithmic average heat transfer coefficient

$\Delta \ell$ = line segment

P = pressure

P_r = Prandtl number

q = heat flux

R = gas constant

T = temperature

T = average temperature

t = time

V = volume

W = weight

$\dot{W}$ = mass flow rate

ϵ = emissivity

σ = radiation constant

Subscripts

g = gas

i = internal

ou = outer

t = at time t

w = wall

0 = at time 0

p = plenum

APPENDIX D. SUPPORTING ANALYSES – VAPORIZING LIQUID SYSTEMS

Regardless of the propellant selected, two major problems are encountered in vaporizing liquids. First, heat of vaporization is usually substantial and must be provided by creation of new thermal energy or from sensible heat somewhere in the vehicle. Second, control of liquid position, especially during the heat exchange process, is complicated by the absence of a gravitational field, particularly during commanded turns.

BATTERY SYSTEM

In the early phases of the study it was noted that waste heat produced by the subsystem or solar illumination, while potentially adequate to vaporize the required quantity of liquid, could not be supplied at an adequate rate for the major maneuvers. Neither could the heat be stored in the spacecraft structure or by means of a practical weight of additional metal. Consequently, the first system examined was an electrically heated vaporizer. Because of the high peak power demanded by the heaters, batteries were required.

Calculations were made for both ammonia and propane propellants. Because propane proved to be the better choice, that system will be described in detail.

The thermodynamic processes involved in the ideal system are illustrated in Figure D-1. The starting point is liquid at 63°F under its vapor pressure. The cycle utilized by the proposed system (1-2-4-5 on the diagram) consists of an isothermal vaporization of the liquid in the vaporizer (1 to 2), an isenthalpic expansion (2 to 4) of the saturated vapor from the vapor pressure at 63°F down to 50 psia, and an isobaric superheating of the gas back to ambient temperature. Since enthalpy is a state function it is acceptable, for computational purposes, to substitute path (2-3-5) for (2-4-5).

Propane has a heat of vaporization of 152 Btu/lb at 63°F. The change in enthalpy for the saturated vapor, on path 2 to 3, is -14.4 Btu/lb.

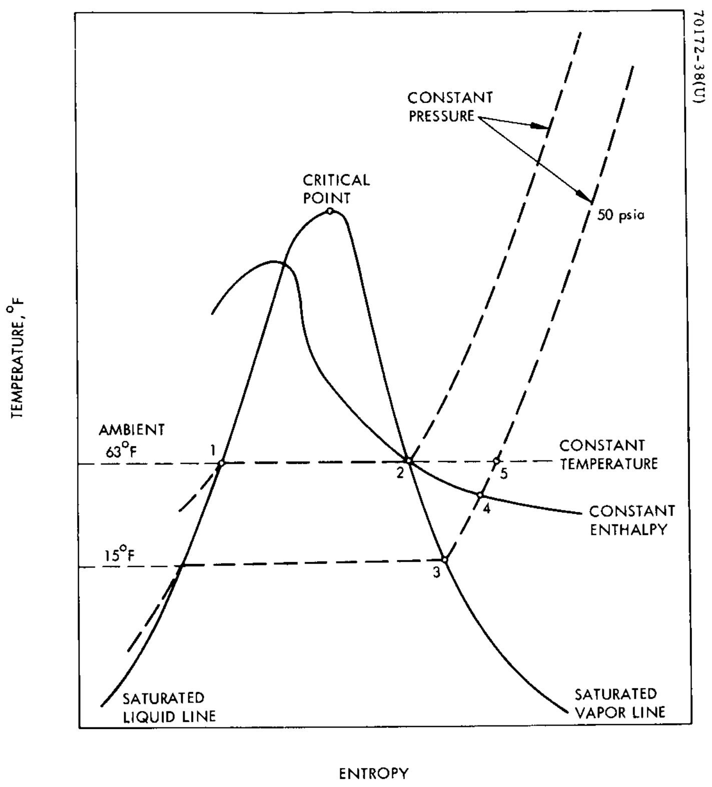


Figure D-1. Propane Cycle

The temperature at point 3 is 15° F, and the specific heat of propane gas is approximately constant at 17.7 Btu/lb mole °F. Hence the enthalpy change for (3 to 5) is:

$$17.7 \text{ Btu/lb-mole-°F} \times (63-14.6)\text{°F} \times \frac{1}{44} \text{ lb/lb-mole} = 19.5 \text{ Btu/lb}$$

The enthalpy change from 1 to 5 is therefore 157 Btu/lb.

Since this system supplies gas to the thrusters at constant conditions (50 psia, 63° F), the specific impulse will not change over the mission. The specific impulse of propane is 73.6 seconds (vacuum, ϵ = 100). Hence, for a single plenum propane system with roll control capability, W_P = 5800 lb-sec/73.6 second = 78.8 pound.

If the maximum permissible prelaunch temperature is 110° F, then the density of liquid propane, 28.85 lb/ft^3, yields a propellant volume of 2.73 cubic feet. A margin of safety is required; a tank volume one percent greater than the maximum possible liquid volume was allowed. The volume of the container is thus 2.76 cubic feet. It was assumed that the minimum drop across the regulator is 20 psi. The final sequence is 200 days of limit cycle operation. In the cruise mode, no power is supplied to the vaporizer and it makes little difference whether gas or liquid is removed from the tank. The mission can thus be successfully completed at the assumed thrust chamber pressure of 50 psia, with a tank of gaseous propane at 70 psia. From the perfect gas law, at an ambient temperature of 63°F, it follows that the weight of gas required is 1.5 pounds and the total propellant needed is

$$W = 78.8 + 1.5$$

$$= 80.3 \text{ pound}$$

Iteration yields a tank volume of 2.80 cubic feet, corresponding to a tank radius of 0.87 foot.

The tank material chosen was 6AL-4V Titanium, which has a yield stress of 160,000 psi and a density of 0.161 lb/in^3. The tank thickness with a safety factor of 2.2 and under maximum pressure is

$$t = \frac{2.2PR}{2\sigma} = 0.00128 \text{ foot}$$

which is larger than the minimum gauge of 0.001 foot. Allowing 25 percent for bosses, flanges, welds, and fittings, the weight of the tank is

$$W = 4\pi R^2 t (\rho \times 1728) = 4.23 \text{ pound}$$

The next step is sizing the batteries. The optimum battery configuration includes a silver-zinc single shot battery for roll control during retro, if this capability is required, and a rechargeable silver-zinc battery for all other maneuvers except limit cycle operation. The amount of power required for a given maneuver is a function of the total impulse, including cross-coupling and the enthalpy change from initial to final state. The number of watt-hours required for the roll control during retro is

$$0.293 \frac{\text{watt-hour}}{\text{Btu}} \times \frac{2148 \text{ lb-sec}}{73.6 \text{ seconds}} \times 157 \frac{\text{BTU}}{\text{lb}}$$

$$= 1334 \text{ watt-hours}$$

The specific power for the one shot battery is 32.5 w-hr/lb. Including an allowance of a 10 percent safety factor, the weight 46.2 pounds. The critical maneuvers for the rechargeable battery occur in the commanded turns and reacquisition of references just before and after retro. These maneuvers require 340 watt-hours. The specific power of a rechargeable battery is 40 w-hr/lb, but combining this with a 50 percent depth of discharge yields an apparent specific power of 20 w-hr/lb. The weight of the rechargeable battery with roll control capability is then 17 pounds.

The final step is the sizing of the various fixed weights necessary and the various arrangements of the system. The weights of the propane system in its various configurations are given in Table D-1. A similar listing for ammonia appears in Table D-2.

REGENERATIVE VAPORIZER-HEAT EXCHANGER

Since the previously described battery systems were too heavy to be attractive, the spacecraft was examined for other sources to supply the required vaporization energy.

The spacecraft structure itself may be considered a heat source, but even if temperature excursions of 100°F were permissible, about 120 pounds of aluminum structure would be needed to supply heat for 500 lb_f-sec of commanded turns and about 480 pounds of aluminum structure to provide roll control during retro. Other metallic structures would be less attractive; e.g., the weight of titanium would be 210 and 820 pounds respectively, or nickel 250 and 980 pounds respectively.

TABLE D-1. PROPANE VAPORIZING LIQUID SYSTEM COMPONENTS AND WEIGHTS, BATTERY VERSION

(Number of components required in parentheses, weight in pounds)

Item	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Propellant															
3600 lb_f sec requirement	49.8	(1) 49.8	(1) 49.8	–	–	(2) 99.6	(2) 99.6	–	–	–	–	(1) 49.8	(1) 49.8	–	–
5400 lb_f sec requirement	74.7	–	–	–	–	–	–	–	–	(2)149.4	–	–	–	–	–
5800 lb_f sec requirement	80.2	–	–	(1) 80.2	(1) 80.2	–	–	(2)160.4	(2)160.4	–	–	–	–	(1) 80.2	(1) 80.2
8700 lb_f sec requirement	120.7	–	–	–	–	–	–	–	–	–	(2)241.4	–	–	–	–
Tank															
3600 lb_f sec capacity	2.62	(1) 2.6	(1) 2.6	–	–	(2) 5.2	(2) 5.2	–	–	–	–	(1) 2.6	(1) 2.6	–	–
5400 lb_f sec capacity	3.92	–	–	–	–	–	–	–	–	(2) 7.8	–	–	–	–	–
5800 lb_f sec capacity	4.23	–	–	(1) 4.2	(1) 4.2	–	–	(2) 8.4	(2) 8.4	–	–	–	–	(1) 4.2	(1) 4.2
8700 lb_f sec capacity	6.33	–	–	–	–	–	–	–	–	–	(2) 12.7	–	–	–	–
Manifolds and bracketry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Fill and vent valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Pressure regulator	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Control valve	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Control valve	1.3	–	–	(16) 20.8	(16) 20.8	–	–	(16) 20.8	(16) 20.8	–	(4) 5.2	–	–	(8) 10.4	(8) 10.4
Heat exchanger	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Nozzle thrust															
0.04	0.06	–	(24) 1.4	–	(24) 1.4	–	(24) 1.4	–	(24) 1.4	–	–	–	(12) 0.7	–	(12) 0.7
0.08	0.06	(12) 0.7	–	(12) 0.7	–	(12) 0.7	–	(12) 0.7	–	(12) 0.7	(12) 0.7	(6) 0.4	–	(6) 0.4	–
0.83	0.09	–	–	–	(8) 0.7	–	–	–	(8) 0.7	–	–	–	–	–	(4) 0.4
1.67	0.13	–	–	(4) 0.5	–	–	–	(4) 0.5	–	–	(4) 0.5	–	–	(2) 0.3	–
Batteries															
Maneuver															
No roll	17.8	(1) 17.8	(1) 17.8	–	–	(2) 35.6	(2) 35.6	–	–	(2) 35.6	–	(1) 17.8	(1) 17.8	–	–
Roll	17.0	–	–	(1) 17.0	(1) 17.0	–	–	(2) 34.0	(2) 34.0	–	(2) 34.0	–	–	(1) 17.0	(1) 17.0
Retro	46.2	–	–	(1) 46.2	(1) 46.2	–	–	(2) 92.4	(2) 92.4	–	(2) 92.4	–	–	(1) 46.2	(1) 46.2
Total fixed weights		52.6	53.3	119.3	120.2	85.3	86.0	197.5	198.4	75.7	172.2	42.7	43.0	98.8	99.2
Total system weight		105.0	105.7	203.7	204.6	190.1	190.8	366.3	367.2	232.9	426.3	95.1	95.4	183.2	183.6

TABLE D-2. AMMONIA VAPORIZING LIQUID SYSTEM COMPONENTS AND WEIGHTS, BATTERY VERSION

(Number of components required in parentheses, weight in pounds)

Item	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Propellant (including 70-psi waste) (total impulse required/thrust)															
3600 lb_f-sec requirement	34.49	(1) 34.5	(1) 34.5	–	–	(2) 69.0	(2) 69.0	–	–	–	–	(1) 34.5	(1) 34.5		
5400 lb_f-sec requirement	51.74	–	–	–	–	–	–	–	–	(2) 103.5	–	–	–	–	–
5800 lb_f-sec requirement	55.57	–	–	(1) 55.6	(1) 55.6	–	–	(2) 111.2	(2) 111.2	–	–	–	–	(1) 55.6	(1) 55.6
8700 lb_f-sec requirement	83.35	–	–	–	–	–	–	–	–	–	(2) 166.7	–	–	–	–
Tank (maximum T = 110°F)															
3600 lb_f-sec capacity	1.124	(1) 1.1	(1) 1.1	–	–	(2) 2.2	(2) 2.2	–	–	–	–	(1) 1.1	(1) 1.1	–	–
5400 lb_f-sec capacity	1.686	–	–	–	–	–	–	–	–	(2) 3.4	–	–	–	–	–
5800 lb_f-sec capacity	1.811	–	–	(1) 1.8	(1) 1.8	–	–	(2) 3.6	(2) 3.6	–	–	–	–	(1) 1.8	(1) 1.8
8700 lb_f-sec capacity	2.716	–	–	–	–	–	–	–	–	–	(2) 5.4	–	–	–	–
Manifolds and bracketry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Fill and vent valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Pressure regulator	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Control valve	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Control valve	1.3	–	–	(16) 20.8	(16) 20.8	–	–	(16) 20.8	(16) 20.8	–	(4) 5.2	–	–	(8) 10.4	(8) 10.4
Heat exchanger	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Nozzles															
0.04 lb_f	0.06	–	(24) 1.4	–	(24) 1.4	–	(24) 1.4	–	(24) 1.4	–	–	–	(12) 0.7	–	(12) 0.7
0.08 lb_f	0.06	(12) 0.7	–	(12) 0.7	–	(12) 0.7	–	(12) 0.7	–	(12) 0.7	(12) 0.7	(6) 0.4	–	(6) 0.4	–
0.83 lb_f	0.09	–	–	–	(8) 0.7	–	–	–	(8) 0.7	–	–	–	–	–	(4) 0.4
1.67 lb_f	0.13	–	–	(4) 0.5	–	–	–	(4) 0.5	–	–	(4) 0.5	–	–	(2) 0.3	–
Batteries															
Maneuver, no roll	42.39	(1) 42.4	(1) 42.4	–	–	(2) 84.8	(2) 84.8	–	–	(2) 84.8	–	(1) 42.4	(1) 42.4	–	–
Maneuver, with roll	40.52	–	–	(1) 40.5	(1) 40.5	–	–	(2) 81.0	(2) 81.0	–	(2) 81.0	–	–	(1) 40.5	(1) 40.5
Retro maneuver	107.5	–	–	(1) 107.5	(1) 107.5	–	–	(2) 215.0	(2) 215.0	–	(2) 215.0	–	–	(1) 107.5	(1) 107.5
Totals															
Propellant and tank		35.6	35.6	57.4	57.4	71.2	71.2	114.8	114.8	106.9	172.1	35.6	35.6	57.4	57.4
Fixed		77.2	77.9	204.1	205.0	134.5	135.2	367.0	367.9	124.9	341.8	66.9	67.2	183.6	184.0
Total system weight		112.8	113.5	261.5	262.4	205.7	206.4	481.8	482.7	231.8	513.9	102.5	102.8	241.0	241.4

At least in the ammonia case, the propellant itself proved to be a suitable heat sink. Liquid ammonia has a larger heat capacity than most metals. Some examples are:

Material	Cp, cal/g °C
Ammonia	1.05
Aluminum	0.215
Titanium	0.125
Nickel	0.106

Calculations were made for the ammonia system only, although it is noted that other propellants might prove more attractive. Propane, water, and several of the Freons should also be examined.

Machine calculations of the propellant requirements were made because of the interdependency of temperature, propellant specific impulse, liquid density, vapor pressure, heat of vaporization, and heat capacities of liquid and gas. These parameters affect both propellant and tankage requirements. At temperatures near the critical point, rapid changes in some of these parameters make hand calculations very difficult.

Certain assumptions were made. First, the cruise mode is not affected by tank temperature because only propellant contained in the downstream distribution system is used for limit cycle pulses. It has been noted that temperature control of the distribution system is required to prevent liquid condensation. It is assumed that a temperature well above saturation would be selected, probably near the spacecraft structure ambient. The temperature specified in these calculations is 70° F.

A second set of assumptions concerns the vaporizer-heat exchanger. The design of an efficient low- or zero-gravity heat exchanger was not within the scope of the study, but two limiting sets of calculations were conducted to set bounds on system weights. In the first case, it was assumed that the heat exchanger was entirely efficient, and the effluent gas was delivered at the highest possible temperature consistent with adiabatic vaporization and equilibration of the gas, liquid propellant, and tank. This temperature is slightly lower than the original tank temperature. The BASIC language computer program for this gas is listed in Figure D-2. In the second set of calculations, it was assumed that the effluent gas reached a temperature no higher than the equilibrium temperature, 38° F, at which liquid ammonia has a vapor pressure of 70 psia, the pressure downstream of the first regulator. The tank heat capacity was assumed not to contribute to the heating process. The second program is listed in Figure D-2b.

In either case the program starts with a selected amount of propellant and initial tank temperature. After calculation of the initial conditions and tank weight, a history of conditions at each point in the mission is compiled. By inspection of several such histories, the amount of propellant required to maintain minimum pressure at all times is determined for several initial temperatures. The results of the calculations for ammonia are shown in Tables D-3 and D-4.

```
1  REM CALCULATES PROPELLANT REQUIREMENTS FOR AN AMMONIA VAPORJET.
5  REM              PERFECT HEAT EXCHANGER, ETC.
10 REM TANK IS 6AL-4V TITANIUM
11 LET D1=.161
12 LET S1=1.6E5
13 LET C1=.1248
15 READ I1
16 LET X=INT(I1/5800)
17 LET X2=1+420/(I1-560)+140/1600
18 LET X1=1+420/(I1-560)
20 READ W0
25 READ T0
27 DATA 3600,34,220
30 LET T0=(T0+459.69)/1.8
31 LET T=T0
32 LET J=0
35 GOSUB 800
40 LET P0=P
50 LET T6=270
55 GOSUB 800
60 LET P6=P
65 LET T=T0
66 LET P=P0
70 LET V=909.113-12.3558*T+.0639511*T↑2-1.45679E-4*T↑3+1.23959E-7*T↑4
78 LET V=.001*V
79 LET V=V/17.01
80 LET V=V*453.59*W0/28.316
85 LET V=1.2*V
90 LET R1=(3*V/4/3.14159)↑(1/3)
100 LET T1=2.2*P0*R1/2/S1
105 IF T1>.001 THEN 110
106 LET T1=.001
110 LET W1=4*3.14159*R1*R1*T1*D1*1728
115 LET W=W0
120 PRINT "INTIAL CONDITIONS"
130 PRINT"NH3="W"LBS"
140 PRINT "P="P0"PSIA","T="T0*1.8-459.69"F","V="V"CU FT"
150 PRINT
160 PRINT "6AL-4V TITANIUM TANK"
170 PRINT "D="2*(R1+T1)"FT","TH="T1*12"IN"
180 PRINT "W="W1 "LBS"
185 LET W1=1.1*W1
187 PRINT  "TANK+10 PERCENT="W1
190 LET X3=0
195 PRINT
196 PRINT
200 PRINT "AFTER INJECTION RATE REMOVAL UP TO CRUISE ONE"
210 LET E=545*X1
211 LET X3=X3+E
220 GOSUB 600
230 PRINT "AFTER FIRST CRUISE"
240 LET E=85*X2
241 LET X3=X3+E
250 GOSUB 750
260 PRINT "AFTER TURNS MIDCOURSE AND REACQUISITION"
270 LET E=(210+X*100)*X1
```

a) Ideal Heat Exchanger

Figure D-2. Ammonia Vaporjet BASIC Language Program – Regenerative Vaporizer

```
271 LET X3=X3+E
280 GOSUB 600
290 PRINT "AFTER SECOND CRUISE"
300 LET E=60*X2
301 LET X3=X3+E
310 GOSUB 750
320 PRINT "AFTER TURNS MIDCOURSE AND REACQUISITION"
330 LET E=(10+50*X)*X1
331 LET X3=X3+E
340 GOSUB 600
350 PRINT "AFTER THIRD CRUISE"
360 LET E=1140*X2
361 LET X3=X3+E
370 GOSUB 750
380 PRINT "AFTER TURN RETRO AND REACQUISITION"
390 LET E=(510+2000*X)*X1
391 LET X3=X3+E
400 GOSUB 600
410 PRINT "AFTER ORBITAL CRUISE"
420 LET E=30*X2
421 LET X3=X3+E
430 GOSUB 750
440 PRINT "AFTER TURNS TRIM AND REACQUISITION"
450 LET E=(165+50*X)*X1
451 LET X3=X3+E
460 GOSUB 600
470 PRINT "END OF MISSION"
480 LET E=285*X2
481 LET X3=X3+E
490 GOSUB 750
500 IF W>17.01*P0*V/10.68/(T0*1.8) THEN 950
510 PRINT
520 PRINT "NO LIQUID REMAINS","P="W*10.68*T0*1.8/17.01/V
530 GO TO 950
600 LET E0=50
605 LET E=E/E0
610 LET T=T0
611 LET H0=(1322.41+.238724*T+.0333658*T*T)*W*453.59+W1*453.59*C1*T
612 LET H0=H0/17.01
615 LET W5=0
620 FOR N=1 TO E0
625 LET I=105*SQR(T*1.8/523)
630 LET W5=W5+E/I
635 LET W=W-E/I
638  LET H6=1322.41+.238724*T6+.0333658*T6*T6
640 LET H1=1322.41+.238724*T+.0333658*T*T
642 LET H1=H1-H6
645 LET H2=5182
650 LET H3=6.5846*(T-T6)+.0061251*(T*T-T6*T6)/2+2.3663E-6*(T↑3-T6↑3)/3
651 LET H3=H3-1.5981E-9*(T↑4-T6↑4)/4
655 LET H=(H2+H3-H1)*453.59*E/I/17.01
656 LET C2=1480.17-19.1763*T+.0939077*T↑2-2.0368E-4*T↑3+1.65412E-7*T↑4
658 LET C2=C2/17.01
659 LET C5=(W+E/I/2)*C2*453.59+W1*C1*453.59
660 LET T8=H/C5
662 LET T=T-T8
665 IF T>T6 THEN 680
```

a) Ideal Heat Exchanger (continued)

Figure D-2 (continued). Ammonia Vaporjet BASIC Language Program — Regenerative Vaporizer

```
67Ø PRINT "CASE NOT PHYSICAL"
675 STOP
68Ø NEXT N
684 LET J=1
685 GO TO 8ØØ
69Ø GO TO 9ØØ
75Ø LET P=PØ
755 LET I=105
762 LET T=TØ
765 LET W5=E/I
77Ø LET W=W-E/I
775 GO TO 9ØØ
8ØØ LET P=9.584586-1648.6Ø68/T-1.638646E-2*T+2.4Ø3276E-5*T↑2
8Ø1 LET P=P-1.1687Ø8E-8*T↑3
8Ø2 LET P=1Ø↑P
81Ø LET P=14.697*P
82Ø IF J=1 THEN 69Ø
83Ø RETURN
9ØØ PRINT "T="1.8*T-459.69"F","P="P"PSIA"
91Ø PRINT "ISP="I"SEC","NH3="W"LBS","DNH3="W5
92Ø PRINT
93Ø PRINT
94Ø RETURN
95Ø PRINT
96Ø PRINT"TOTAL IMPULSE="X3
999 END
```

a) Ideal Heat Exchanger (continued)

Figure D-2 (continued). Ammonia Vaporjet BASIC Language Program — Regenerative Vaporizer *

*BASIC language programs were written for the General Electric Model 235 computer. Additional information concerning this system is available from General Electric Company, 13430 North Black Canyon Highway, Phoenix, Arizona.

```
1   REM CALCULATES PROPELLANT REQUIREMENTS FOR AN AMMONIA VAPORJET.

1Ø  REM TANK IS 6AL-4V TITANIUM
11  LET D1=.161
12  LET S1=1.6E5
13  LET C1=.1248
15 READ I1
16 LET X=INT(I1/58ØØ)
17 LET X2=1+42Ø/(I1-56Ø)+14Ø/16ØØ
18 LET X1=1+42Ø/(I1-56Ø)
2Ø  READ WØ
25  READ TØ
27 DATA 58ØØ,75,2ØØ
3Ø  LET TØ=(TØ+459.69)/1.8
31  LET T=TØ
32  LET J=Ø
35  GOSUB 8ØØ
4Ø LET PØ=P
5Ø LET T6=27Ø
55  GOSUB 8ØØ
6Ø  LET P6=P
65 LET T=TØ
66 LET P=PØ
7Ø  LET V=9Ø9.113-12.3558*T+.Ø639511*T↑2-1.45679E-4*T↑3+1.23959E-7*T↑4
78  LET V=.ØØ1*V
79  LET V=V/17.Ø1
8Ø  LET V=V*453.59*WØ/28.316
85 LET V=1.2*V
9Ø  LET R1=(3*V/4/3.14159)↑(1/3)
1ØØ LET T1=2.2*PØ*R1/2/S1
1Ø5 IF T1>.ØØ1 THEN 11Ø
1Ø6 LET T1=.ØØ1
11Ø LET W1=4*3.14159*R1*R1*T1*D1*1728
115 LET W=WØ
12Ø PRINT "INTIAL CONDITIONS"
13Ø PRINT"NH3="W"LBS"
14Ø PRINT "P="PØ"PSIA","T="TØ*1.8-459.69"F","V="V"CU FT"
15Ø PRINT
16Ø PRINT "6AL-4V TITANIUM TANK"
17Ø PRINT "D="2*(R1+T1)"FT","TH="T1*12"IN"
18Ø PRINT "W="W1 "LBS"
185 LET W1=1.1*W1
187 PRINT  "TANK+1Ø PERCENT="W1
19Ø LET X3=Ø
195 PRINT
196 PRINT
2ØØ PRINT "AFTER INJECTION RATE REMOVAL UP TO CRUISE ONE"
21Ø LET E=545*X1
211 LET X3=X3+E
22Ø GOSUB 6ØØ
23Ø PRINT "AFTER FIRST CRUISE"
24Ø LET E=85*X2
241 LET X3=X3+E
25Ø GOSUB 75Ø
26Ø PRINT "AFTER TURNS MIDCOURSE AND REACQUISITION"
27Ø LET E=(21Ø+X*1ØØ)*X1
```

b) Non-Ideal Heat Exchanger

Figure D-2 (continued). Ammonia Vaporjet BASIC Language Program — Regenerative Vaporizer

```
271 LET X3=X3+E
280 GOSUB 600
290 PRINT "AFTER SECOND CRUISE"
300 LET E=60*X2
301 LET X3=X3+E
310 GOSUB 750
320 PRINT "AFTER TURNS MIDCOURSE AND REACQUISITION"
330 LET E=(10+50*X)*X1
331 LET X3=X3+E
340 GOSUB 600
350 PRINT "AFTER THIRD CRUISE"
360 LET E=1140*X2
361 LET X3=X3+E
370 GOSUB 750
380 PRINT "AFTER TURN RETRO AND REACQUISITION"
390 LET E=(510+2000*X)*X1
391 LET X3=X3+E
400 GOSUB 600
410 PRINT "AFTER ORBITAL CRUISE"
420 LET E=30*X2
421 LET X3=X3+E
430 GOSUB 750
440 PRINT "AFTER TURNS TRIM AND REACQUISITION"
450 LET E=(165+50*X)*X1
451 LET X3=X3+E
460 GOSUB 600
470 PRINT "END OF MISSION"
480 LET E=285*X2
481 LET X3=X3+E
490 GOSUB 750
500 IF W>17.01*P0*V/10.68/(T0*1.8) THEN 950
510 PRINT
520 PRINT "NO LIQUID REMAINS","P="W*10.68*T0*1.8/17.01/V
530 GO TO 950
600 LET T0=T
604 LET I=96
606 LET W5=E/I
610 LET H0=(1322.41+.238724*T0+.0333658*T0↑2)*W*453.59/17.01
614 LET W=W-W5
618 LET H2=240000*W5
622 LET H1=H0-H2
625 LET T2=T0
626 LET Z=(H1*17.01)/(W*453.59*.0333658)
627 LET T1=SQR((Z)-((1322.41+.238724*T2)/.0333658))
630 IF ABS((T2-T1)/T2)<1.0E-5 THEN 642
634 LET T2=T1
638 GO TO 627
642 LET T=T1
663 IF T1>270 THEN 684
670 PRINT "CASE NOT PHYSICAL"
684 LET J=1
685 GO TO 800
690 GO TO 900
750 LET P=P0
755 LET T=T0
760 LET I=105
765 LET W5=E/I
```

b) Non-Ideal Heat Exchanger (continued)

Figure D-2 (continued). Ammonia Vaporjet BASIC Language Program — Regenerative Vaporizer

70172-40C (U)

```
770 LET W=W-W5
775 GO TO 900
800 LET P=9.584586-1648.6068/T-1.638646E-2*T+2.403276E-5*T↑2
801 LET P=P-1.168708E-8*T↑3
802 LET R=10↑P
810 LET P=14.697*P
820 IF J=1 THEN 690
830 RETURN
900 PRINT "T="1.8*T-459.69"F","P="P"PSIA"
910 PRINT "ISP="I"SEC","NH3="W"LBS","DNH3="W5
920 PRINT
930 PRINT
940 RETURN
950 PRINT
960 PRINT"TOTAL IMPULSE="X3
999 END
```

b) Non-Ideal Heat Exchanger (continued)

Figure D-2 (continued). Ammonia Vaporjet BASIC Language Program — Regenerative Vaporizer

TABLE D-3. AMMONIA AND TANK REQUIREMENTS, EFFICIENT HEAT EXCHANGER

Configuration	Initial Temperature, °F	Propellant Weight, pounds	Tank Weight, pounds	Total, pounds
No added roll control, 3600 lb_f-sec	180	40.0	6.0	46.0
	200	37.2	7.5	44.7
	210	36.0	8.4	44.4
	220	34.9	9.6	44.5
With added roll control, 5800 lb_f-sec	180	105.3	15.8	121.1
	200	93.3	18.8	112.0
	220	83.3	22.7	106.0

TABLE D-4. AMMONIA AND TANK REQUIREMENTS, INEFFICIENT HEAT EXCHANGER

Configuration	Initial Temperature, °F	Propellant Weight, pounds	Tank Weight, pounds	Total, pounds
No added roll control, 3600 lb_f-sec	180	42.0	6.3	48.3
	200	39.7	8.0	47.7
	220	37.8	10.3	48.1
With added roll control, 5800 lb_f-sec	180	109.3	16.5	125.8
	200	97.7	19.7	117.4
	220	88.6	24.2	112.8

These results show that it is desirable to have tank temperature near 220° F, but efficient heating of the vaporized gas is not very important. Minimum total weight appears to occur well below the 270° F critical temperature, at least for the smaller plenums. The larger plenums may benefit from a temperature slightly higher than 220° F but not much because vapor pressure and tank weight increase rapidly.

Total system weights are shown in Table D-5. The systems are much more attractive than those which included batteries. The smaller plenums which do not provide the added roll control are light enough to be considered competitive with the other systems analyzed in the study.

EXTERNAL HEAT EXCHANGER

The regenerative vaporizer-heat exchanger improved the ammonia system considerably but the weights, particularly for the larger plenum systems, were still considerably heavier than the better hydrazine plenum system weights.

One other possible energy source, the main propulsion or retro-engine, may be considered. A substantial amount of waste heat is produced during the time the engines are firing. It would not be possible to store such heat for any appreciable time, but fortunately this event coincides with the peak demand time of the large plenum systems. It is thought that very little hazard or degradation in main engine performance could result from low pressure heat exchange tubing wrapped around the combustion chamber or nozzle. More study of potential interface problems should be made.

It has not been within the scope of the present study to examine other spacecraft subsystems, so no information is available concerning the main or retro-engines. Consequently, it is not possible to evaluate available heat flux or define a specific heat exchanger design. It is possible, however, to make parametric calculations with respect to gas temperature. Such calculations have been made assuming the gas temperature at the added roll control thrusters would be between 200 and 1000° F. As shown in Figure D-4, a modification of the previous computer program was written in which the roll control thrusters were operated at a selected temperature, but all other functions were performed by a regenerative heat exchanger. The results appear in Table D-6. Note that only those systems having added roll control capability are applicable.

The most striking conclusion is that system weight is relatively unaffected by temperature in the external heat exchanger. With the tank initially at 220° F, it would appear that the external heat exchanger might be required to do little more than simply vaporize the liquid.

TABLE D-5. AMMONIA VAPORIZING LIQUID SYSTEM REGENERATIVE VERSION COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Item	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Propellant															
3600 lb_f-sec requirement	36.0	(1) 36.0	(1) 36.0	—	—	(2) 72.0	(2) 72.0	—	—	(2)*108.0	—	(1) 36.0	(1) 36.0	—	—
5800 lb_f-sec requirement	83.3	—	—	(1) 83.3	(1) 83.3	—	—	(2)166.6	(2)166.6	—	(2)*249.9	—	—	(1) 83.3	(1) 83.3
Tank															
3600 lb_f-sec capacity	8.4	(1) 8.4	(1) 8.4	—	—	(2) 16.8	(2) 16.8	—	—	(2)* 25.2	—	(1) 8.4	(1) 8.4	—	—
5800 lb_f-sec capacity	22.7	—	—	(1) 22.7	(1) 22.7	—	—	(2) 45.4	(2) 45.4	—	(2)* 68.1	—	—	(1) 22.7	(1) 22.6
Manifolds and bracketry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Fill and vent valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Pressure regulator	5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(4) 20.0	(4) 20.0	(4) 20.0	(4) 20.0	(4) 20.0	(4) 20.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0
Control valve	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Control valve	1.3	—	—	(16) 20.8	(16) 20.8	—	—	(16) 20.8	(16) 20.8	—	(4) 5.2	—	—	(8) 10.4	(8) 10.4
Insulation	2.0	(1) 2.0	(1) 2.0	(1) 2.0	(1) 2.0	(2) 4.0	(2) 4.0	(2) 4.0	(2) 4.0	(2.5) 5.0	(2.5) 5.0	(1) 2.0	(1) 2.0	(1) 2.0	(1) 2.0
Nozzle thrust, lb_f															
0.04	0.06	—	(24) 1.4	—	(24) 1.4	—	(24) 1.4	—	(24) 1.4	—	—	—	(12) 0.7	—	(12) 0.7
0.08	0.06	(12) 0.7	—	(12) 0.7	—	(12) 0.7	—	(12) 0.7	—	(12) 0.7	(12) 0.7	(6) 0.4	—	(6) 0.4	—
0.63	0.09	—	—	—	(8) 0.7	—	—	—	(8) 0.7	—	—	—	—	—	(4) 0.4
1.67	0.13	—	—	(4) 0.5	—	—	—	(4) 0.5	—	—	(4) 0.5	—	—	(2) 0.3	—
Thermostat and heater	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Totals															
Fixed weight		39.8	40.5	61.1	62.0	59.7	60.4	81.0	81.9	51.1	56.8	29.9	30.2	40.6	41.0
Propellant and tank		44.4	44.4	106.0	106.0	88.8	88.8	212.0	212.0	133.0	318.0	44.4	44.4	106.0	106.0
Total system weight		84.2	84.9	167.1	168.0	148.5	149.2	293.0	293.9	184.1	374.8	74.3	74.6	146.6	147.0

*1-1/2 times needed propellant

```
1  REM CALCULATES PROPELLANT REQUIREMENTS FOR AN AMMONIA VAPORJET.
5  REM EXTERNAL HEAT EXCHANGER FOR ROLL CONTROL.  ENTER DATA AS
6  REM   27 DATA NH3 WT., TANK TEMP IN F., HEAT EX TEMP F.,
10 REM TANK IS 6AL-4V TITANIUM
11 LET D1=.161
12 LET S1=1.6E5
13 LET C1=.1248
15 LET I1=5800
16 LET X=0
17 LET X2=1+420/(I1-560)+140/1500
18 LET X1=1+420/(I1-560)
20 READ W0
25 READ T0
26 READ T9
27 DATA 45,220,1000
30 LET T0=(T0+459.69)/1.8
31 LET T=T0
32 LET J=0
35 GOSUB 800
40 LET P0=P
50 LET T6=270
55 GOSUB 800
60 LET P6=P
65 LET T=T0
66 LET P=P0
70 LET V=909.113-12.3558*T+.0639511*T↑2-1.45579E-4*T↑3+1.23959E-7*T↑4
78 LET V=.001*V
79 LET V=V/17.01
80 LET V=V*453.59*W0/28.316
85 LET V=1.2*V
90 LET R1=(3*V/4/3.14159)↑(1/3)
100 LET T1=2.2*P0*R1/2/S1
105 IF T1>.001 THEN 110
106 LET T1=.001
110 LET W1=4*3.14159*R1*R1*T1*D1*1728
115 LET W=W0
120 PRINT "INTIAL CONDITIONS"
130 PRINT"NH3="W"LBS"
140 PRINT "P="P0"PSIA","T="T0*1.8-459.69"F","V="V"CU FT"
150 PRINT
160 PRINT "6AL-4V TITANIUM TANK"
170 PRINT "D="2*(R1+T1)"FT","TH="T1*12"IN"
180 PRINT "W="W1 "LBS"
185 LET W1=1.1*W1
187 PRINT  "TANK+10 PERCENT="W1
190 LET X3=0
195 PRINT
196 PRINT
210 LET E=545*X1
211 LET X3=X3+E
220 GOSUB 600
240 LET E=85*X2
241 LET X3=X3+E
250 GOSUB 750
270 LET E=(210+X*100)*X1
271 LET X3=X3+E
280 GOSUB 600
```

Figure D-3. Ammonia Vaporjet BASIC Language Program — External Heat Exchanger

```
281 LET W=W-1Ø7.4/(125*SQR((T9+46Ø)/523))
3ØØ LET E=6Ø*X2
3Ø1 LET X3=X3+E
31Ø GOSUB 75Ø
33Ø LET E=(1Ø+53*X)*X1
331 LET X3=X3+E
34Ø GOSUB 6ØØ
341 LET W=W-53.7/(125*SQR((T9+46Ø)/523))
36Ø LET E=114Ø*X2
361 LET X3=X3+E
37Ø GOSUB 75Ø
38Ø PRINT "AFTER TURN RETRO AND REACQUISITION"
39Ø LET E=(51Ø+2ØØØ*X)*X1
391 LET X3=X3+E
4ØØ GOSUB 6ØØ
4Ø1 GOSUB 9ØØ
4Ø2  LET W=W-2148.4/(125*SQR((46Ø+T9)/523))
42Ø LET E=3Ø*X2
421 LET X3=X3+E
43Ø GOSUB 75Ø
44Ø PRINT "AFTER TURNS TRIM AND REACQUISITION"
45Ø LET E=(165+5Ø*X)*X1
451 LET X3=X3+E
46Ø GOSUB 6ØØ
461 GOSUB 9ØØ
47Ø PRINT "END OF MISSION"
48Ø LET E=285*X2
481 LET X3=X3+E
49Ø GOSUB 75Ø
495 GOSUB 9ØØ
5ØØ IF W>17.Ø1*P3*V/1Ø.68/(TØ*1.8) THEN 95Ø
51Ø PRINT
52Ø PRINT "NO LIQUID REMAINS","P="W*1Ø.68*TØ*1.8/17.Ø1/V
53Ø GO TO 95Ø
6ØØ LET EØ=5Ø
6Ø5 LET E=E/EØ
61Ø LET T=TØ
611 LET HØ=(1322.41+.238724*T+.Ø333658*T*T)*W*453.59+W1*453.59*C1*T
612 LET HØ=HØ/17.Ø1
615 LET W5=Ø
62Ø FOR N=1 TO EØ
625 LET I=125*SQR(T*1.8/523)
63Ø LET W5=W5+E/I
635 LET W=W-E/I
638  LET H6=1322.41+.238724*T6+.Ø333658*T6*T6
64Ø LET H1=1322.41+.238724*T+.Ø333658*T*T
642 LET H1=H1-H6
645 LET H2=5182
65Ø LET H3=6.5846*(T-T6)+.Ø351251*(T*T-T6*T6)/2+2.3663E-6*(T↑3-T6↑3)/3
651 LET H3=H3-1.5981E-9*(T↑4-T6↑4)/4
655 LET H=(H2+H3-H1)*453.59*E/I/17.Ø1
656 LET C2=148Ø.17-19.1763*T+.Ø939Ø77*T↑2-2.Ø358E-4*T↑3+1.65412E-7*T↑4
658 LET C2=C2/17.Ø1
659 LET C5=(W+E/I/2)*C2*453.59+W1*C1*453.59
66Ø LET T8=H/C5
662 LET T=T-T8
665 IF T>T6 THEN 68Ø
67Ø PRINT "CASE NOT PHYSICAL"
```

Figure D-3 (continued). Ammonia Vaporjet BASIC Language Program – External Heat Exchanger

```
575 STOP
580 NEXT N
584 LET J=1
685 GO TO 800
690 RETURN
750 LET P=P0
755 LET I=1.05
760 LET I=105
762 LET T=T0
765 LET W5=E/I
770 LET W=W-E/I
775 RETURN
800 LET P=9.584586-1648.6068/T-1.638646E-2*T+2.403276E-5*T↑2
801 LET P=P-1.168708E-8*T↑3
802 LET P=10↑P
810 LET P=14.697*P
820 IF J=1 THEN 690
830 RETURN
900 PRINT "T="1.8*T-459.69"F","P="P"PSIA"
910 PRINT "ISP="I"SEC","NH3="W"LBS","DNH3="W5
920 PRINT
930 PRINT
940 RETURN
950 PRINT
960 PRINT "TOTAL IMPULSE="X3
999 END
```

Figure D-3 (continued). Ammonia Vaporjet BASIC Language Program — External Heat Exchanger

One case of interest is that for which the propellant tank initially is at a nominal ambient temperature, thereby simplifying temperature conditioning. It is seen, however, that considerable extra propellant still is required to maintain minimum tank temperature. A system summary, on the assumption of a 500°F exit temperature of the external heat exchanger, is given in Table D-7.

TABLE D-6. AMMONIA VAPORJET, EXTERNAL HEAT EXCHANGER

Initial Tank Temperature	Roll Thruster Temperature, °F	Propellant Weight, pounds	Tank Weight, pounds	Total, pounds
220°F	63	53.4	14.6	68.0
	200	51.0	13.9	64.9
	400	48.6	13.2	61.8
	600	46.8	12.8	59.8
	800	45.6	12.4	58.0
	1000	44.8	12.3	57.1
70°F	500	103.7	3.3	107.0

TABLE D-7. AMMONIA VAPORIZING LIQUID SYSTEM, EXTERNAL HEAT EXCHANGER VERSION (ATTACHED TO MIDCOURSE OR RETRO MOTOR) COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Item	Unit Weight	1c	1d	2c	2d	3b	4c	4d
Propellant (220° F)		47.0	47.0	94.0	94.0	141.0	(1) 47.0	(1) 47.0
Tank		(1) 13.0	(1) 13.0	(2) 26.0	(2) 26.0	(2) 39.0	(1) 13.0	(1) 13.0
Manifolds and Bracketing	3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0
Fuel and vent valve	1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0
Filter	1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6
Pressure regulator	5.0	(2) 10.0	(2) 10.0	(4) 20.0	(4) 20.0	(4) 20.0	(2) 10.0	(2) 10.0
Control valve	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(24) 9.6	(24) 9.6	(24) 9.6
Control valve	1.3	(16) 20.8	(16) 20.8	(16) 20.8	(16) 20.8	(4) 5.2	(8) 10.4	(8) 10.4
External heat exchanger	3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0
Nozzles		—	—	—	—	—	—	—
0.04 lb_f	0.06	—	(24) 1.4	—	(24) 1.4	—	—	(12) 0.7
0.08 lb_f	0.06	(12) 0.7	—	(12) 0.7	—	(12) 0.7	(6) 0.4	—
0.83 lb_f	0.09	—	(8) 0.7	—	(8) 0.7	—	—	(4) 0.4
1.67 lb_f	0.13	(4) 0.5	—	(4) 0.5	—	(4) 0.5	(2) 0.3	—
Tank insulation	2.0	(1) 2.0	(1) 2.0	(2) 4.0	(2) 4.0	(2) 5.0	(1) 2.0	(1) 2.0
Thermoswitch and heaters	1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0
Totals								
Propellant and tank		60.0	60.0	120.0	120.0	180.0	60.0	60.0
Fixed weight		64.0	65.0	87.0	88.0	63.0	43.6	44.0
Total system weight		124.0	125.0	207.0	208.0	243.0	103.6	104.0

Note: Configurations 1a, 1b, 2a, 2b, 3d, 4a, and 4b are not considered, since no external heat is available at any time during system operation.

APPENDIX E. SUPPORTING ANALYSES – ELECTROLYSIS PLENUM SYSTEMS

It has been concluded that basic design features of both the water and the hydrazine electrolysis cells will be identical. A baseline design will be described and materials of construction for cells and tankage discussed. A computer program has been written to assist calculations of the propellant storage requirements and ullage conditions.

ELECTROLYSIS CELLS

Electrolysis cell research has been reviewed. The results, reported in Appendix J, indicate that a number of organizations have been active, primarily in studying oxygen-generating cells for life support systems. However, the particular requirements of these cells have introduced complexities, notably in liquid feed and gas separation systems, which are thought to be unnecessary for use in attitude control propulsion systems.

BASELINE DESIGN

None of the manufacturers has examined the simple mixed gas cell which is proposed here as the baseline design, primarily because it would not be acceptable for life support apparatus. In a Hughes independent research and development effort, data have been obtained which indicate that the concept is feasible for zero-g application. One of the cells built was similar to the design shown schematically in Figure E-1.

The principal features of this cell are similar for water electrolysis and for hydrazine electrolysis; materials of construction may be different, however, because of differing compatibilities.

The electrolysis cell proper is composed of two concentric cylinders fabricated from metal screens of appropriate material. The electrodes are separated by a matrix of insulating material which holds the liquid to be electrolyzed by capillary action. Liquid is fed to the matrix by a wick extending through the center of the tank; this wick is not necessarily formed from the same material as the cell matrix. Holes for the escape of the gas generated at the anode are placed in the wick near the cell and in the insulator supporting the cell. The size of the cell would be determined from the required rate of electrolysis, the current density, and the component weights.

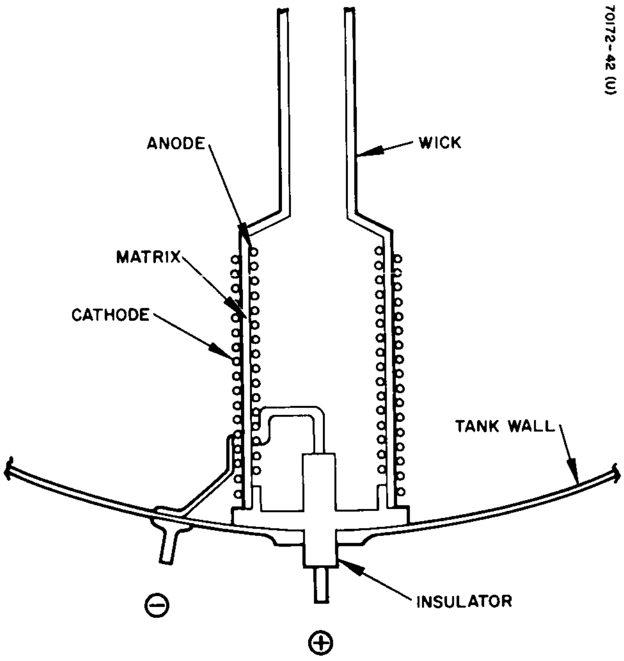


Figure E-1. Electrolysis Cell Design

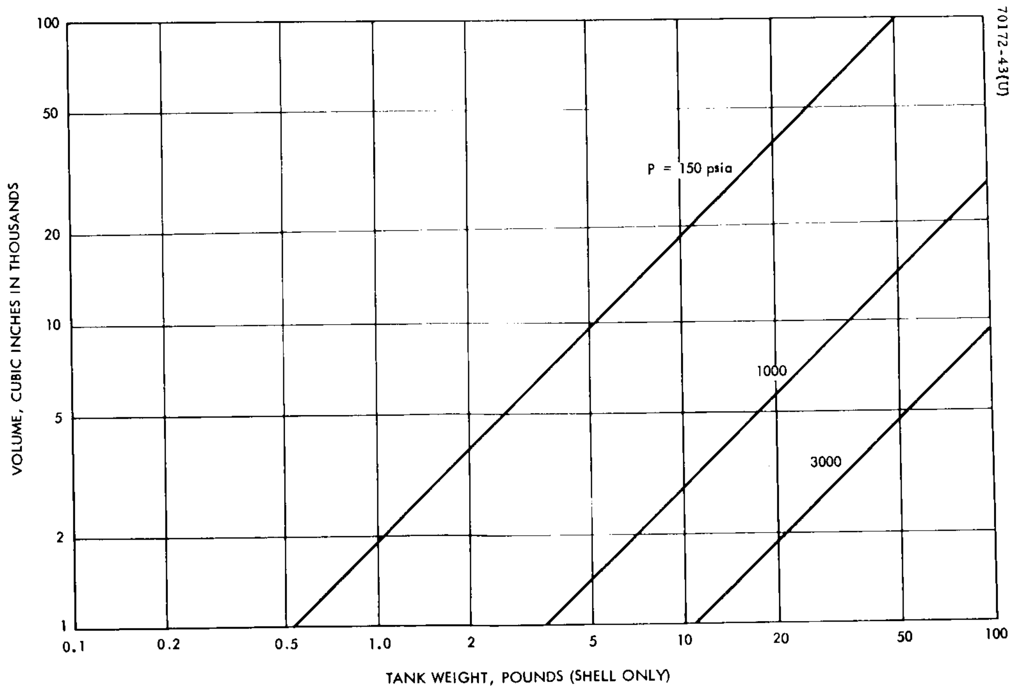


Figure E-2. Maraging Steel Volume Versus Weight

Liquid feed to the electrolysis cell during zero-g operation requires that the liquid stored in the tank be in contact with the wick. This can be accomplished by dividing the tank (assumed to be spherical, oblate spheroid, or cylindrical) into segments by baffles. Capillary forces cause the liquid to fill the apex angle formed by intersecting baffles; the wick is therefore located along the intersection of the baffles for the length of the tank.

Removal of the gases from the tank under zero-g conditions for thruster operation is analogous to the problem of venting excessive pressure from propellant tanks. Fortunately many materials of construction which are neither wetted by hydrazine nor water are available for use in a venting device. It is tentatively concluded that an arrangement of tubes capped by screens of nonwetting material will perform the required function. Further discussion can be found in Appendix J.

MATERIALS AND DESIGN

Basic design considerations are almost identical for the water and hydrazine electrolysis cells. The tankage and lines will be based on the previous Hughes design for a water electrolysis rocket, which utilizes potassium hydroxide as the electrolyte.

In NASA contract NAS S-3828 a material compatibility study was performed in which four metal alloys were exposed to extended electrolyzing conditions. It was concluded that either 18-7-5 maraging steel or 17-4 steel was satisfactory, but that 4130 or 6Al-4V titanium was not. A parametric comparison of maraging steel tank weights is shown in Figure E-2.

Hydrazine tankage would be 6Al-4V titanium. If MIL grade hydrazine without electrolyte were used, a variety of stainless, aluminum, or titanium vessels should be satisfactory.

If an electrolyte were added to the hydrazine, an element of uncertainty would be introduced. No definitive mixture compatibility information was available at the time of this report. The system will operate satisfactorily, however, without electrolyte at the calculated generation rates.

PROPELLANT USAGE

Because of the complexity of the mission and variation in system conditions, computer programs were written for design of the hydrazine electrolysis, dual-mode hydrazine, and water electrolysis systems. The program is written to include the option of prepressurization with Freon 14 in addition to electrolysis gas. It is also possible to analyze systems containing only Freon.

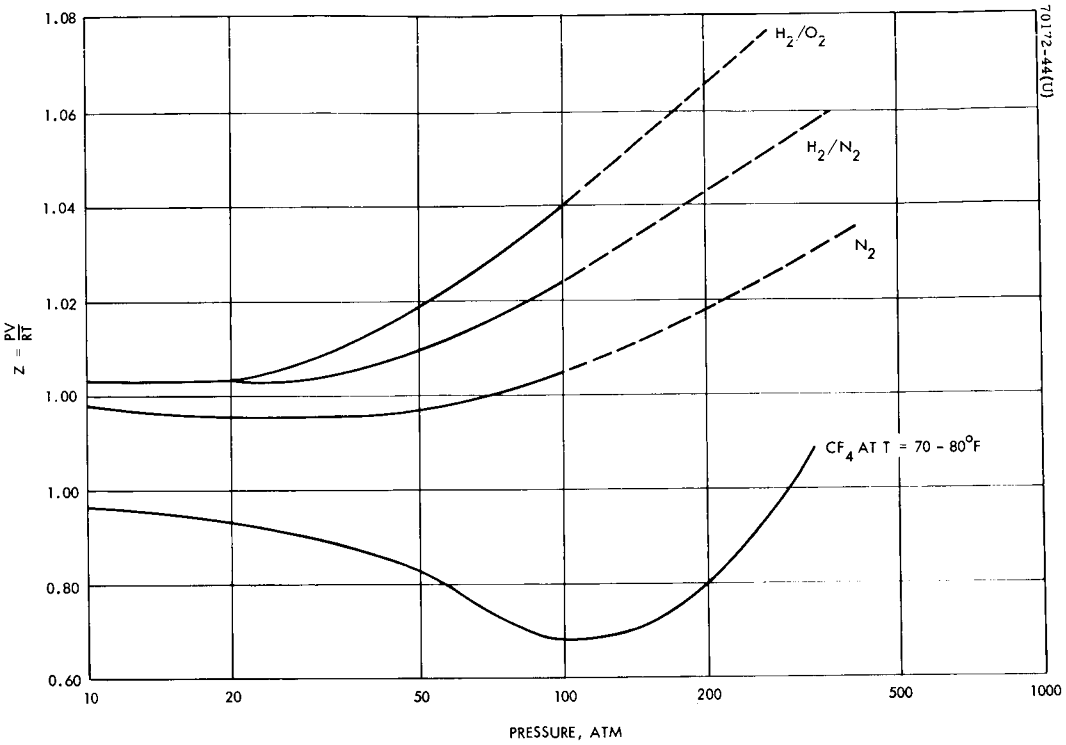


Figure E-3. Gas Compressibilities

Initial pressure, propellant composition, electrolysis rate, and temperature are inputs. Before and after all major events, pressure, volume, weight of remaining propellants, mole fractions, and specific impulse are calculated. Adiabatic cooling of the gas during blowdown is considered for all maneuvers except cruise, for which the small amounts of gas required are assumed to be at ambient temperature. All gas flow is regulated.

A decision was made to use a regulator in the electrolysis plenum systems, because propellant performance loss caused by temperature drop in the expansion process across the regulator arises only from Joule-Thompson cooling and is small enough to be neglected. The loss due to adiabatic blowdown in the tank is present in any case. It therefore appears that the advantages of constant pressure and constant thrust operation outweigh the slight decrease in reliability caused by the presence of the additional component in the system. In order to ensure proper operation of a pressure regulator with a 50 psia outlet, a conservative minimum inlet pressure of 200 psia has been imposed. It is therefore necessary to choose initial conditions compatible with this requirement so that at no time during the mission is system pressure allowed to fall below 200 psia. An optimum system is one in which the minimum pressure reached at any point of the mission is equal to, but not less than the minimum required regulator inlet pressure. This would produce the lightest possible system for a given electrolysis rate.

Specific impulse and compressibility data were calculated by means of existing programs which fit least squares polynomials to bivariant data. By taking the average compressibility based on the mole fraction of the products of hydrazine electrolysis, a compressibility equation was derived in terms of the pressure and composition of the electrolysis gas and CF_4. The compressibility data shown in Figure E-3 were taken from DuPont literature on Freon 14.* The same technique was used in deriving specific impulse of mixtures. By averaging the specific heats and molecular weights of the gases for various percent compositions, the specific impulse equation was derived in terms of gas composition.

It is assumed that during each cruise one pulse occurs every 1/2 hour. Gas generation continues steadily between pulses. Since electrolysis gas is being produced faster than it is being used for cruise corrections, tank pressure eventually reaches the initial launch condition. At this point, the program maintains a constant tank pressure by replacing only the amount of gas used for the remainder of the cruise. This simulates the pressure regulation by the control loop between the tank pressure switch and the electrolysis power supply.

---

*Hilsenrath, et al., Tables of Thermodynamic and Transport Properties of Air, Argon, Carbon Dioxide, Carbon Monoxide, Hydrogen, Nitrogen, Oxygen and Steam, Pergamon, 1960.

The allocation for leakage and cross coupling was proportionally distributed over the mission profile according to the relative size of each maneuver. This simplifying assumption made it possible to tax each maneuver separately and include the additional total impulse in the chronological sequence of calculations.

At the end of the program, tank size, thickness and weight were calculated for a given material with specified physical properties.

Several different programs were developed for analysis of the various systems, including both water and hydrazine electrolysis. Since the difference between water and hydrazine involves only a change in input constants and the "no added roll" case is a simplified version of the "added roll" capability case, only the latter program listing is shown in Figure E-4. A sample output of the Fortran IV program is shown in Figure E-5.

Optimization of the electrolysis systems requires the investigation of a range of ullage conditions and electrolysis rates. The relationship between electrolysis rate and minimum system pressure is nonlinear and, above a limiting value of current, the increased electrolysis rate has a negligible effect on minimum pressure. Reduction below this current results in a rapid decrease in minimum pressure. As the amount of initial pressurant is increased, the electrolysis rate (current) for which minimum pressure is reached during the mission decreases. These effects may be observed in Figure E-6 which shows some typical results. In all cases investigated to date, the required current is no more than 3 to 5 amperes. With this modest requirement, there is little incentive to reduce current by increasing the weight of stored gas. Calculations of average power requirements appear in Appendix F.

Neither the water nor the hydrazine electrolysis plenum system is attractive if maneuvering or roll control during retro is required. Although either system is eminently suitable for providing attitude control propulsion, larger impulse bits require a very large supply of stored gas, and hence an excessive increase in tankage volume and weight. In principle, it is possible to generate the required gas on demand but, depending on electrolyte concentration, at least several kilowatts of power would be required.

One method, prepressurization with Freon 14, has been investigated for reducing the system weight. A significant savings is realized because of the high Freon density and favorable compressibility. Nevertheless, even the most favorable case weighs only about 10 percent less than a simple Freon 14 cold gas (no electrolysis) system. The added complexity of the electrolysis apparatus would definitely preclude selection of such systems.

One other special case, the combustion water rocket, is of interest. Calculations were made by substituting the proper performance parameters into the computer programs. System weights are attractive but do not offer any savings over the other best systems. The water rocket may not be recommended at this time because technology is still in an early stage of development and a satisfactory estimate of reliability is difficult.

```
$       IDENT     366,56243,2395A,01556,ANNE
$       OPTION    FORTRAN
$       FORTRAN LSTOU,DECK
$       INCODE    IBMF
CCRUISE      CRUISE
      SUBROUTINE CRUISE(J1)
      COMMON A,B,X,Y,P,T1  ,H,A1,B1,V,P1,P2,G,BO,R,D,Z,N1,E1,N,E,T,VO,C,
     1R1,TO,H1
      DIMENSION E1(20),T1(20),N1(20)
      DO 20 I = 1,N
      J       = 0
      A1      = A
      B1      = B
      A       = A-A*E/(Y*(A+B))
      B       = B-B*E/(Y*(A1+B)) + T*R
      BO      = BO+T*R
      V       = V+T*R/D
      X       = A/(88.01*(A/88.01+B/10.68))
      Y  = 119.949 - 339.835*X + 973.961*X**2 - 1609.92*X**3 + 1338.61*
     1X**4 - 432.739*X**5
   10 P1      = P
      Z       = X*(1.00886-3.135E-4*P1 + 6.03801E-8* P1**2 +1.57454E-11*
     1          P1**3) + X*(-2.90002E-15*P1**4) + (1.-X)*(.9997+1.68728E-
     2          5 *P1) + (1.-X)*(-6.8728E-10*P1**2)
      P       = (A/88.01+B/10.68)*10.73*H*Z/V
      P2      = P
      YY      = ABS(P2-P1)/G
      NN      = I
      IF(YY .GT. 1.E-5) GO TO 10
      IF(J1 .NE. 7) GO TO 16
      XL7    = ((P/Z - (200./(1. - .05*X)))/(P/Z))*(A+B)*Y
      XN1     = N-I
      XN1     =(XN1)*E
      IF(XL7 .GE. XN1) GO TO 77
   16 IF(P .GE. G ) GO TO 30
   20 CONTINUE
      GO TO 60
   30 NH      = NN/2
      WRITE (2,40) NN,NH,P
   40 FORMAT(10X4H N = I6    ,2X29HFULL PRESSURE WAS REACHED AT,I6   ,2X,
     117HHOURS CRUISE, P = E15.6//)
      P       = G
      DO 50 I =NN,N
      A1      = A
      A       = A-A*E/(Y*(A+B))
      B       = B-B*E/(Y*(A1+B))
      B1      = B
      B       = 10.68*((G*V/(10.73*H*Z))-A/88.01)
      V       = V + (B-B1)/D
      CALL HETGAS
      IF(J1 .NE. 7) GO TO 50
      XL7    = ((P/Z - (200./(1. - .05*X)))/(P/Z))*(A+B)*Y
      XN2     = N-I
      XN2     = XN2*E
      IF(XL7 .GE. XN2) GO TO 77
   50 CONTINUE
   60 H1      = H
```

Figure E-4. Fortran IV Program — Hydrazine Electrolysis With Added Roll Control

```
      WRITE (2,15)
   15 FORMAT(7X1HA,14X1HB,14X1HX,14X1HV,14X1HP,13X3HISP,13X1HT//)
      WRITE (2,35) A, B, X, V, P, Y, H1
   35 FORMAT (7E15.6/)
      VX      = (V-VO)*D
   77 WRITE (2,26) VX
   26 FORMAT(/10X32HCUMULATIVE LIQUID ELECTROLYZED = E15.6//)
      IF(J1 .NE. 7) GO TO 88
      NX      = NN/2
      WRITE (2,70) NX,P
   70 FORMAT(10X25H ELECTROLYSIS STOPPED AT,I5,3X,16H HOURS CRUISE,P=E15
     1.6//)
      A1      = A
      A       = A - A*XL7/Y/(A+B)
      B       = B - B*XL7/Y/(A1+B)
      X       = A/88.01/(A/88.01 + B/10.68)
      P       = (A/88.01+B/10.68)*10.73*H*(1.-.05*X)/V
   75 FORMAT(7E15.6/)
      WRITE (2,74)
      WRITE (2,75) A,B,X,H,Y,P
   74 FORMAT(7X1HA,14X1HB,14X1HX,14X1HH,13X3HISP,14X1HP//)
   88 RETURN
      END
$      FORTRAN LSTOU,DECK
$      INCODE  IBMF
CHETGAZ     HETGAZ
      SUBROUTINE HETGAZ
      COMMON A,B,X,Y,P,T1  ,H,A1,B1,V,P1,P2,G,BO,R,D,Z,N1,E1,N,E,T,VO,C,
     1R1,TO,H1
      DIMENSION E1(20),T1(20),N1(20)
      B       = B+T*R
      BO      = BO+T*R
      V       = V+T*R/D
      X       = A/(88.01*(A/88.01+B/10.68))
      Y   = 119.949 - 339.835*X + 973.961*X**2 - 1609.92*X**3 + 1338.61*
     1X**4 - 432.739*X**5
   20 P1      = P
      Z       = X*(1.00886-3.135E-4*P1 + 6.03801E-8*P1**2 + 1.57454E-11*
     1          P1**3)+ X*(-2.90002E-15*P1**4)+ (1.-X) * (.9997+1.68728E-
     2          5 *P1) + (1.-X)*(-6.8728E-10*P1**2)
      P       = (A/88.01 + B/10.68)*10.73*H*Z/V
      P2      = P
      YY      = ABS(P2-P1)/G
      IF(YY  .GT. 1.E-5 ) GO TO 20
      H1      = H
      WRITE (2,30)
   30 FORMAT(7X1HA,14X1HB,14X1HX,14X1HV,14X1HP,13X3HISP,13X1HT//)
      WRITE (2,35) A, B, X, V, P, Y, H1
   35 FORMAT(7E15.6/)
      VX      = (V-VO)*D
      WRITE (2,40) VX
   40 FORMAT(10X32HCUMULATIVE LIQUID ELECTROLYZED = E15.6//)
      RETURN
      END
$      FORTRAN LSTOU,DECK
$      INCODE  IBMF
CHETGAS     HETGAS
```

Figure E-4 (continued). Fortran IV Program — Hydrazine Electrolysis With Added Roll Control

```
      SUBROUTINE HETGAS
      COMMON A,B,X,Y,P,T1 ,H,A1,B1,V,P1,P2,G,BO,R,D,Z,N1,E1,N,E,T,VO,C,
     1R1,TO,H1
      DIMENSION E1(20),T1(20),N1(20)
      X       = A/(88.01*(A/88.01+B/10.68))
      Y  = 119.949 - 339.835*X + 973.961*X**2 - 1609.92*X**3 + 1338.61*
     1X**4 - 432.739*X**5
      P1      = P
      Z       = X*(1.00886-3.135E-4*P1 + 6.03801E-8*P1**2 + 1.57454E-11*
     1           P1**3)+ X*(-2.90002E-15*P1**4) + (1.-X)*(.9997+1.68728E-5
     2           *P1) + (1.-X)*(-6.8728E-10*P1**2)
      RETURN
      END
$      FORTRAN LSTOU,DECK
$      INCODE  IBMF
CHETGSZ     HETGSZ
      SUBROUTINE HETGSZ
      COMMON A,B,X,Y,P,T1 ,H,A1,B1,V,P1,P2,G,BO,R,D,Z,N1,E1,N,E,T,VO,C,
     1R1,TO,H1
      DIMENSION E1(20),T1(20),N1(20)
      X       = A/(88.01*(A/88.01+B/10.68))
      Y  = 119.949 - 339.835*X + 973.961*X**2 - 1609.92*X**3 + 1338.61*
     1X**4 - 432.739*X**5
   20 P1      = P
      Z       = X*(1.00886-3.135E-4*P1 + 6.03801E-8*P1**2 + 1.57454E-11*
     1           P1**3)+ X*(-2.90002E-15*P1**4)+ (1.-X) * (.9997+1.68728E-
     2           5 *P1) + (1.-X)*(-6.8728E-10*P1**2)
      P       = (A/88.01 + B/10.68)*10.73*H*Z/V
      P2      = P
      YY      = ABS(P2-P1)/G
      IF(YY .GT. 1.E-5 ) GO TO 20
      RETURN
      END
CXPRINT     XPRINT
      SUBROUTINE XPRINT (I)
      COMMON A,B,X,Y,P,T1 ,H,A1,B1,V,P1,P2,G,BO,R,D,Z,N1,E1,N,E,T,VO,C,
     1R1,TO,H1
      DIMENSION E1(20),T1(20),N1(20)
      GO TO (1,10,20,30,40,50,60,70,80,90,100,110,120,130,140,150),I
    1 WRITE (2,5)
    5 FORMAT(10X26HBEFORE ACQUISITION OF REFS//)
      GO TO 200
   10 WRITE (2,15)
   15 FORMAT(10X26HAFTER ACQUISITION OF REFS //)
      GO TO 200
   20 WRITE (2,25)
   25 FORMAT(10X26HBEFORE ROLL CALIBRATION    //)
      GO TO 200
   30 WRITE (2,35)
   35 FORMAT(10X26HAFTER ROLL CALIBRATION     //)
      GO TO 200
   40 WRITE (2,45)
   45 FORMAT(10X27H BEFORE CANOPUS ACQUISITION//)
      GO TO 200
   50 WRITE (2,55)
   55 FORMAT(10X27HAFTER CANOPUS ACQUISITION  //)
      GO TO 200
```

Figure E-4 (continued). Fortran IV Program — Hydrazine Electrolysis With Added Roll Control

```
   60 WRITE (2,65)
   65 FORMAT(10X19HBEFORE FIRST CRUISE//)
      GO TO 200
   70 WRITE (2,75)
   75 FORMAT(10X19HAFTER FIRST CRUISE //)
      GO TO 200
   80 WRITE (2,85)
   85 FORMAT(10X39HAFTER TURNS,MIDCOURSE AND REACQUISITION//)
      GO TO 200
   90 WRITE (2,95)
   95 FORMAT(10X19HAFTER SECOND CRUISE//)
      GO TO 200
  100 WRITE (2,105)
  105 FORMAT(10X40HAFTER SECOND MIDCOURSE AND REACQUISITION//)
      GO TO 200
  110 WRITE (2,115)
  115 FORMAT(10X19HAFTER THIRD CRUISE //)
      GO TO 200
  120 WRITE (2,125)
  125 FORMAT(10X40HAFTER TURNS,RETRO AND REACQUISITION      //)
      GO TO 200
  130 WRITE (2,135)
  135 FORMAT(10X26HAFTER FIRST ORBITAL CRUISE//)
      GO TO 200
  140 WRITE (2,145)
  145 FORMAT(10X28HAFTER ORBITAL TRIM,TURN,ETC.//)
      GO TO 200
  150 WRITE (2,155)
  155 FORMAT(10X14HEND OF MISSION//)
  200 RETURN
      END
$      FORTRAN LSTOU,DECK
$      INCODE  IBMF
CMAIN        MAIN
      COMMON A,B,X,Y,P,T1 ,H,A1,B1,V,P1,P2,G,BO,R,D,Z,N1,E1,N,E,T,VO,C,
     1R1,TO,H1
      DIMENSION E1(20),T1(20),N1(20)
C     REM JPL MISSION ANALYSIS
C     REM HYDRAZINE,ALL COLD GAS
      READ (1,10) H,D
      WRITE(2,110)
  110 FORMAT (1H1)
   10 FORMAT(6E12.6)
      READ (1,10) (E1(I),I=1,13)
      READ (1,10) (T1(I),I=1, 9)
      READ (1,11) (N1(I),I=1, 5)
   11 FORMAT (12I6)
   20 READ (1,10) G,C,A,B
      WRITE(2,22) G,C,A,B
   22 FORMAT(10X18HINITIAL CONDITIONS/10X3HG = F8.3,2X13HPSIA PRESSURE/
     110X3HC = F8.3,2X25HAMPS ELECTROLYSIS CURRENT/10X3HA = F8.3,
     22X12HPOUNDS FREON/10X3HB = F8.3,2X23HPOUNDS ELECTROLYSIS GAS//)
      R      = (3600.*32.05/(96520.*4.*453.59))*C
      P      = G
      J1     = 0
      CALL HETGAS
      V      = (A/88.01+B/10.68)*10.73*H*Z/G
```

Figure E-4 (continued). Fortran IV Program — Hydrazine Electrolysis With Added Roll Control

```
      BO      = B
      VO      = V
      WRITE(2,24) V,R
   24 FORMAT(10X4H V = E15.6,2X12HCU FT VOLUME//10X4H R = E15.6,2X26HLBS
     1/HR GAS PRODUCTION RATE//10X28HAFTER INJECTION RATE REMOVAL//)
      II      = 1
      DO 40 I=1,4
      E       = E1(I)
      CALL SSFIRE (IBIT)
      CALL XPRINT (II)
      T       = T1(I)
      CALL HETGAZ
      J       =II+1
      CALL XPRINT (J)
      N       = I
      II      = II+2
   40 CONTINUE
      II      = 9
      I2      = 5
      J3      = 1
      DO 50 I= 5,11,2
      N       = N1(J3)
      E       = E1(I)
      T       = T1(I2)
      CALL CRUISE (J1)
      CALL XPRINT (II)
      J       = I+1
      E       = E1(J)
      CALL SSFIRE (IBIT)
      IF(IBIT .EQ. 1) GO TO 20
      CALL HETGSZ
      I1      = II+1
      CALL XPRINT(I1)
      K       = I
      J3      = J3+1
      II      = II+2
      I2      = I2+1
   50 CONTINUE
      N       = N1(5)
      E       = E1(13)
      T       = T1(9)
      J1      = 7
      CALL CRUISE (J1)
      WRITE (2,70)
   70 FORMAT(//10X 4HTANK//)
      R1      = (1.05*V/4.189)**(1./3.)
      TO      = 2.2*G*R1/320000.
      TT1      = .001
      IF(TO .GT. TT1 ) GO TO 80
      TO      = TT1
   80 TT      = TO*12.
      RR      = 2.*(R1+TO)
      RD      = (V-VO)*D
      WRITE(2,90) TT,RR
   90 FORMAT(10X11HTHICKNESS = E15.6,2X6HINCHES,10X10HDIAMETER = E15.6,
     12X3H FT//)
      W1      = 1.1*12.57*R1**2*TO*.161*1728.
```

Figure E-4 (continued). Fortran IV Program — Hydrazine Electrolysis With Added Roll Control

```
      WRITE(2,100) W1,RD,BO
  100 FORMAT(10X29H6AL-4V TITANIUM TANK WEIGHT = E15.6,2X3HLBS//
     110X21HLIQUID ELECTROLYZED = E15.6,2X3HLBS//10X17HTOTAL PROPELLANT=
     2 E15.6,2X3HLBS//)
      WRITE (2,110)
      GO TO 20
      END
$       FORTRAN LSTOU,DECK
$       INCODE  IBMF
CSSFIRE      SSFIRE
      SUBROUTINE SSFIRE (IBIT)
      COMMON A,B,X,Y,P,T1 ,H,A1,B1,V,P1,P2,G,BO,R,D,Z,N1,E1,N,E,T,VO,C,
     1R1,TO,H1
      DIMENSION E1(20),T1(20),N1(20)
      IBIT    = 0
      EO      = 100.
      IE      = 100
      E       = E/EO
      DO 20 K=1,IE
      A1      = A+B
      A       = A-A*E/(Y*A1)
      B       = B-B*E/(Y*A1)
      X       = A/(88.01*(A/88.01+B/10.68))
      C1      = (1.-X)*(1.9872*(2.*3.469+3.5083)/3.)+X*.169*88.01
      XK      = C1/(C1-1.9872)
      IF(A) 100,125,125
  125 IF(B) 100,150,150
  100 IBIT    = 1
      WRITE (2,160)
  160 FORMAT(10X,15HA OR B NEGATIVE//)
      GO TO 200
  150 P       = P*(((A+B)/A1)**XK)
      CALL HETGAS
      H1      = P*V/10.73/Z/(A/88.01+B/10.68)
      Y       = Y*SQRT(H1/H)
   20 CONTINUE
      WRITE (2,30)
   30 FORMAT(7X1HA,14X1HB,14X1HX,14X1HV,14X1HP,13X3HISP,13X1HT//)
      WRITE (2,35) A, B, X, V, P, Y, H1
   35 FORMAT(7E15.6/)
  200 RETURN
      END
```

Figure E-4 (continued). Fortran IV Program — Hydrazine Electrolysis With Added Roll Control

70172-46(U)

INITIAL CONDITIONS
G =1500.000 PSIA PRESSURE
C = 5.000 AMPS ELECTROLYSIS CURRENT

A = 107.000 POUNDS FREON
B = 0. POUNDS ELECTROLYSIS GAS

V = 0.328609E 01 CU FT VOLUME

R = 0.329427E-02 LBS/HR GAS PRODUCTION RATE

GENERAL SYMBOLS:
X = MOLE FRACTION OF CF_4
P = TANK PRESSURE (PSIA)
I_{SP} = SPECIFIC IMPULSE (LB_f-SEC/LB_m)
(CALCULATED AT FINAL TEMPERATURE OF MANEUVER)
T = FINAL TEMPERATURE OF MANEUVER -(°R)
(INITIAL TEMPERATURE = 530°R)

AFTER INJECTION RATE REMOVAL

A	B	X	V	P	ISP	T
0.103228E 03	0.	0.100000E 01	0.328609E 01	0.143913E 04	0.497375E 02	0.523905E 03

BEFORE ACQUISITION OF REFS

A	B	X	V	P	ISP	T
0.103228E 03	0.329427E-02	0.999737E 00	0.328614E 01	0.145411E 04	0.500341E 02	0.530000E 03

CUMULATIVE LIQUID ELECTROLYZED = 0.329280E-02

AFTER ACQUISITION OF REFS

A	B	X	V	P	ISP	T
0.102785E 03	0.328015E-02	0.999737E 00	0.328614E 01	0.144692E 04	0.499989E 02	0.529254E 03

BEFORE ROLL CALIBRATION

A	B	X	V	P	ISP	T
0.102785E 03	0.115158E-01	0.999078E 00	0.328627E 01	0.144982E 04	0.500544E 02	0.530000E 03

CUMULATIVE LIQUID ELECTROLYZED = 0.115276E-01

AFTER ROLL CALIBRATION

A	B	X	V	P	ISP	T
0.952197E 02	0.106682E-01	0.999078E 00	0.328627E 01	0.132736E 04	0.493996E 02	0.516224E 03

BEFORE CANOPUS ACQUISITION

A	B	X	V	P	ISP	T
0.952197E 02	0.213746E-01	0.998154E 00	0.328644E 01	0.135892E 04	0.500826E 02	0.530000E 03

CUMULATIVE LIQUID ELECTROLYZED = 0.222335E-01

AFTER CANOPUS ACQUISITION

A	B	X	V	P	ISP	T
0.948881E 02	0.213002E-01	0.998154E 00	0.328644E 01	0.135345E 04	0.500518E 02	0.529348E 03

BEFORE FIRST CRUISE

A	B	X	V	P	ISP	T
0.948881E 02	0.221237E-01	0.998082E 00	0.328646E 01	0.135499E 04	0.500848E 02	0.530000E 03

CUMULATIVE LIQUID ELECTROLYZED = 0.230553E-01

AFTER FIRST CRUISE

A	B	X	V	P	ISP	T
0.930472E 02	0.791614E 00	0.934485E 00	0.329877E 01	0.143806E 04	0.515511E 02	0.530000E 03

CUMULATIVE LIQUID ELECTROLYZED = 0.799713E 00

AFTER TURNS,MIDCOURSE AND REACQUISITION

Figure E-5. Hydrazine Electrolysis Sample Printout

N = 801 FULL PRESSURE WAS REACHED AT, 400 HOURS CRUISE, P = 0.150009E 04

A	B	X	V	P	ISP	T
0.837854E 02	0.205686E 01	0.831739E 00	0.332041E 01	0.150000E 04	0.531136E 02	0.530000E 0

CUMULATIVE LIQUID ELECTROLYZED = 0.216576E 01

AFTER SECOND MIDCOURSE AND REACQUISITION

A	B	X	V	P	ISP	T
0.825637E 02	0.202687E 01	0.831739E 00	0.332041E 01	0.147439E 04	0.529987E 02	0.527709E 03

AFTER THIRD CRUISE

N = 104 FULL PRESSURE WAS REACHED AT, 52 HOURS CRUISE, P = 0.150001E 04

A	B	X	V	P	ISP	T
0.618482E 02	0.403821E 01	0.650174E 00	0.336656E 01	0.150000E 04	0.571652E 02	0.530000E 03

CUMULATIVE LIQUID ELECTROLYZED = 0.507732E 01

AFTER TURNS,RETRO AND REACQUISITION

A	B	X	V	P	ISP	T
0.118718E 02	0.775137E 00	0.650174E 00	0.336656E 01	0.208114E 03	0.448144E 02	0.325721E 03

AFTER FIRST ORBITAL CRUISE

A	B	X	V	P	ISP	T
0.113506E 02	0.112778E 01	0.549820E 00	0.337281E 01	0.374938E 03	0.605312E 02	0.530000E 03

CUMULATIVE LIQUID ELECTROLYZED = 0.547223E 01

AFTER ORBITAL TRIM,TURN,ETC.

A	B	X	V	P	ISP	T
0.768620E 01	0.763688E 00	0.549820E 00	0.337281E 01	0.233613E 03	0.574562E 02	0.477519E 03

END OF MISSION

CUMULATIVE LIQUID ELECTROLYZED = 0.547223E 01

ELECTROLYSIS STOPPED AT, 262 HOURS CRUISE,P= 0.379886E 03

A	B	X	H	ISP	P
0.386689E 01	0.826188E 00	0.362231E 00	0.530000E 03	0.684747E 02	0.200000E 03

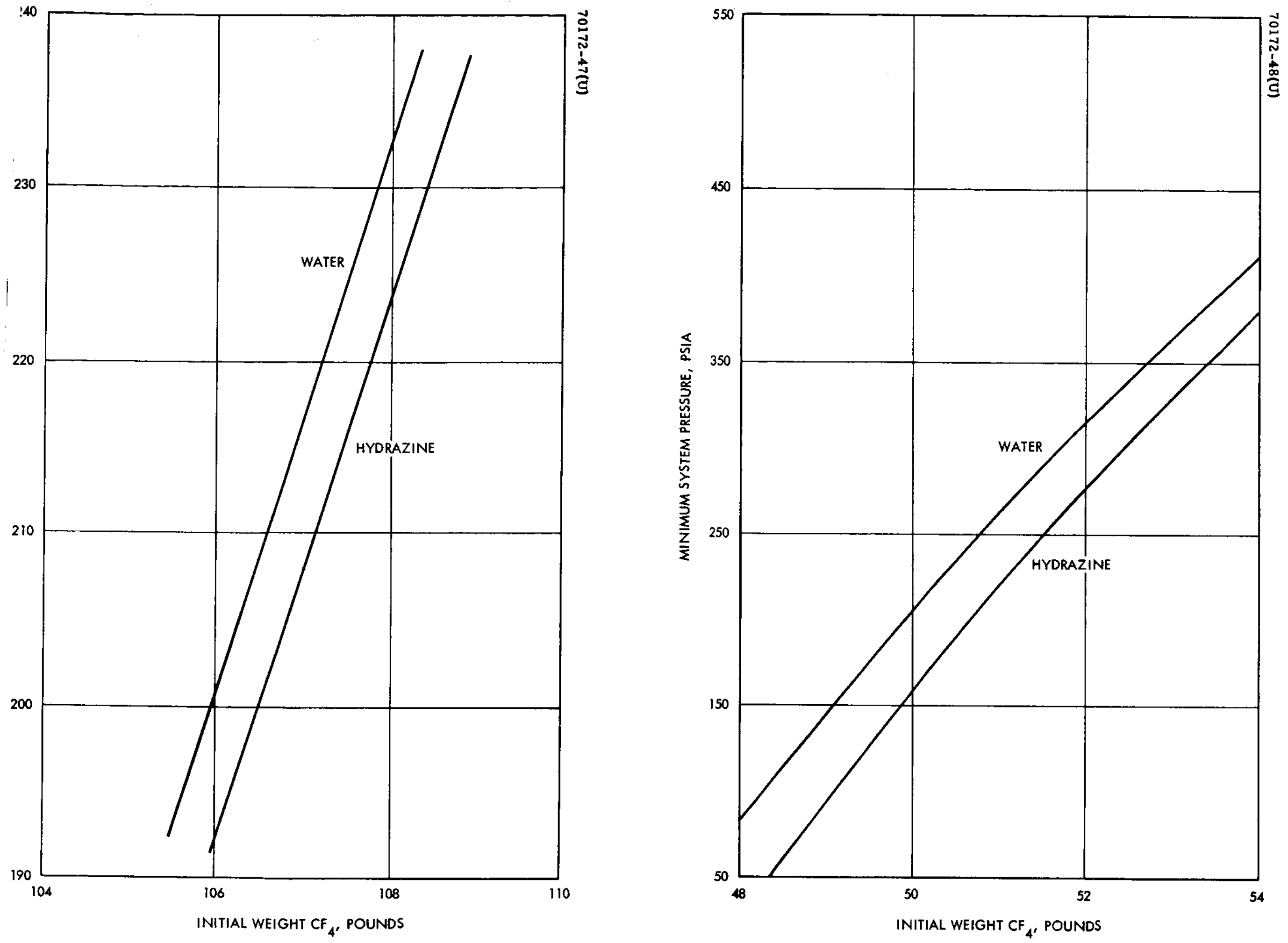
TANK

THICKNESS = 0.117172E 00 INCHES DIAMETER = 0.191321E 01 FT

6AL-4V TITANIUM TANK WEIGHT = 0.336738E 02 LBS

LIQUID ELECTROLYZED = 0.633609E 01 LBS

Figure E-5 (continued). Hydrazine Electrolysis Sample Printout



a) Added Roll Control System

b) No Added Roll Control System

Figure E-6. Electrolysis Systems
Minimum System Pressure
Versus Initial Pressurant
Weight at 5 Amperes Current

SYSTEM WEIGHTS

Detailed listings of components and weights for the electrolysis systems and the combustion water rocket appear in Tables E-1 through E-3.

TABLE E-1. WATER ELECTROLYSIS PLENUM SYSTEM COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Component	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Fill valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Control valve															
Cruise	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Retro	1.4	—	—	(16) 22.4	(16) 22.4	—	—	(16) 22.4	(16) 22.4	—	(4) 5.6	—	—	(8) 11.2	(8) 11.2
Regulator (with relief valve)	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Pressure switch	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Pressure transducer	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Thruster															
0.08 lb_f	0.06	(12) 0.72	—	(12) 0.72	—	(12) 0.72	—	(12) 0.72	—	(12) 0.72	(12) 0.72	(6) 0.4	—	(6) 0.4	—
0.04 lb_f	0.06	—	(24) 1.44	—	(24) 1.44	—	(24) 1.44	—	(24) 1.44	—	—	—	(12) 0.7	—	(12) 0.7
0.83 lb_f	0.09	—	—	—	(8) 0.72	—	—	—	(8) 0.72	—	—	—	—	—	(4) 0.4
1.67 lb_f	0.13	—	—	(4) 0.52	—	—	—	(4) 0.52	—	—	(4) 0.52	—	—	(2) 0.3	—
Electrolysis cell	0.5	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5
Zero-g device	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Manifolds and bracketry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Total fixed weight		37.8	38.5	60.7	61.7	94.2	94.9	78.6	79.6	41.3	47.4	27.9	28.2	39.4	39.8
Propellant		10.3	10.3	7.3	7.3	20.6	20.6	14.6	14.6	20.7	21.8	10.3	10.3	7.3	7.3
Pressurant (CF_4)		50.0	50.0	106.0	106.0	100.0	100.0	212.0	212.0	150.0	318.0	50.0	50.0	106.0	106.0
Tankage		17.4	17.4	34.5	34.5	34.8	34.8	69.0	69.0	52.1	105.2	17.4	17.4	34.5	34.5
Total system weight		115.5	116.2	208.5	209.5	249.6	250.3	373.9	375.1	264.1	492.4	105.6	105.9	187.2	187.6

TABLE E-2. HYDRAZINE ELECTROLYSIS PLENUM SYSTEM COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Component	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Fill valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	1.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(2) 2.0	(1) 1.0	(1) 1.0	(1) 1.0	(1) 1.0
Control valve															
Cruise	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Retro	1.4	—	—	(16) 22.4	(16) 22.4	—	—	(16) 22.4	(16) 22.4	—	(4) 5.6	—	—	(8) 11.2	(8) 11.2
Regulator (with relief valve)	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Pressure switch	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Pressure transducer	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Thruster															
0.08	0.06	(12) 0.72	—	(12) 0.72	—	(12) 0.72	—	(12) 0.72	—	(12) 0.72	(12) 0.72	(6) 0.4	—	(6) 0.4	—
0.04	0.06	—	(24) 1.44	—	(24) 1.44	—	(24) 1.44	—	(24) 1.44	—	—	—	(12) 0.7	—	(12) 0.7
0.83	0.09	—	—	—	(8) 0.72	—	—	—	(8) 0.72	—	—	—	—	—	(4) 0.4
1.67	0.13	—	—	(4) 0.52	—	—	—	(4) 0.52	—	—	(4) 0.52	—	—	(2) 0.3	—
Electrolysis cell	0.5	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5
Zero-g device	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Manifolds and bracketry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Total fixed weight		37.8	38.5	60.7	61.7	94.2	94.9	78.6	79.6	41.3	47.4	27.9	28.2	39.4	39.8
Propellant		9.1	9.1	6.3	6.3	18.2	18.2	12.6	12.6	27.4	19.0	9.1	9.1	6.3	6.3
Pressurant		51.0	51.0	107.0	107.0	102.0	102.0	214.0	214.0	143.0	321.0	51.0	51.0	107.0	107.0
Tankage		17.0	17.0	33.7	33.7	34.0	34.0	67.4	67.4	52.2	101.6	17.0	17.0	33.7	33.7
Total system weight		114.9	115.6	207.7	208.7	248.4	249.1	372.6	373.6	273.9	489.0	105.0	105.3	186.4	186.8

TABLE E-3. COMBUSTION WATER ROCKET COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Component	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Fill valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Start valve	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—
Control valve															
Cruise	0.4	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Retro	1.4	—	—	(16) 22.4	(16) 22.4	—	—	(16) 22.4	(16) 22.4	—	(4) 5.6			(8) 11.2	(8) 11.2
Regulator (with relief valve)	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Pressure switch	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Pressure transducer	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.50	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Filter	1.6	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(2) 3.2	(1) 1.6	(1) 1.6	(1) 1.6	(1) 1.6
Nozzle thrust lb_f															
0.08	0.5	(12) 6.0	—	(12) 6.0	—	(12) 6.0	—	(12) 6.0	—	(12) 6.0	(12) 6.0	(6) 3.0	—	(6) 3.0	—
0.04	0.5	—	(24) 6.0	—	(24) 12	—	(24) 12	—	(24) 12	—	—	—	(12) 6.0		(12) 6.0
0.83	1.5	—		—	(8) 12	—		—	(8) 12	—	—	—	—		(4) 6.0
1.67	2.0	—		(4) 8.0		—		(4) 8.0		—	(4) 8.0	—	—	(2) 4.0	—
Electrolysis cell	0.5	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5
Zero-g device	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Manifolds and bracketry	3.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(2) 6.0	(1) 3.0	(1) 3.0	(1) 3.0	(1) 3.0
Total fixed weight		42.1	48.1	72.5	82.5	59	65	88.4	98.4	44.6	58.2	29.5	32.5	44.7	49.7
Propellant		(1) 12.1	(1) 12.1	(1) 20	(1) 20	(2) 24.2	(2) 24.2	(2) 40.0	(2) 40.0	(2) 20.6	(2) 25.2	(1) 12.1	(1) 12.1	(1) 20	(1) 20
Pressurant		(1) 2.8	(1) 2.8	(1) 11.2	(1) 11.2	(2) 5.6	(2) 5.6	(2) 22.4	(2) 22.4	(2) 5.5	(2) 22.4	(1) 2.8	(1) 2.8	(1) 11.2	(1) 11.2
Tankage		(1) 10.4	(1) 10.4	(1) 37.6	(1) 37.6	(2) 20.8	(2) 20.8	(2) 75.2	(2) 75.2	(2) 21.1	(2) 76.5	(1) 10.4	(1) 10.4	(1) 37.6	(1) 37.6
Total system weight		67.4	73.4	141.3	151.3	109.6	115.6	226	236	91.8	182.3	54.8	57.8	113.5	118.5

APPENDIX F. SUPPORTING ANALYSES — DUAL-MODE HYDRAZINE SYSTEMS

In the dual-mode hydrazine system all steady-state maneuvers are accomplished with liquid monopropellant and all pulsing with the cold gas electrolysis products. On eliminating the steady-state gas requirements, the ullage required for the dual system is considerably smaller than the ullage volumes in the electrolysis systems. Since further reduction of this volume by prepressurization with Freon 14 is insignificant, only nitrogen/ hydrogen electrolysis gas pressurization is considered.

A nominal value of 150 psia was selected for the system operating pressure. In order to maintain thrust levels within the specified ±20 percent, minimum system pressure was chosen as 90 psia. These limits are satisfactory for operation of the liquid engines and eliminate the requirement for a liquid pressure regulator. The gas thrusters also will operate within required limits without a pressure regulator.

The computer program used to analyze the dual system is a modification of the program written for the electrolysis systems. The primary difference is that steady state maneuvers are accomplished by liquid hydrazine so that the amount of liquid used and the accompanying changes in ullage conditions must be calculated. The accumulative amounts of liquid expelled as monopropellant and electrolyzed for pressurization and/or gas expulsion are tabulated. A BASIC language computer program is shown in Figure F-1.

The method for sizing the dual-mode system is identical to that used for the all-gas electrolysis system. With the small ullages required, pressure is extremely sensitive to changes in the electrolysis rate. Therefore, small variations in current result in large changes in the initial ullage requirement. A limit is reached at which a minimum practical ullage is required. Negligible weight savings on tank and propellant are realized by further reduction of ullage volume. A plot of minimum system pressure versus initial pressurant weight is shown in Figure F-2.

```
1    REM JPL MISSION ANALYSIS
2    REM HYDRAZINE DUAL SYSTEM   (NO ROLL CONTROL)
4    PRINT
5    LET H=530
10   LET D=63.1
20   READ G,C,A,B
25   PRINT "INITIAL CONDITIONS:"
30   PRINT G"PSIA PRESSURE"
35   PRINT C"AMPS ELECTROLYSIS CURRENT"
40   PRINT A"POUNDS FREON"
45   PRINT B"POUNDS ELECTROLYSIS GAS"
50   LET R=(3600*32.05/(96520*4*453.59))*C
55   LET P=G
60   LET J=3
65   GO TO 740
70   LET V=(A/88.01+B/10.68)*10.73*H*Z/G
71   LET B3=B
72   LET B0=0
73   LET V0=V
74  LET B2=0
75   PRINT V"CU FT VOLUME"
80   PRINT
85   PRINT R"LBS/HR GAS PRODUCTION RATE"
90   PRINT
95   PRINT "AFTER INJECTION RATE REMOVAL"
100 LET E=201.31
105 GOSUB 600
110 PRINT "BEFORE ACQUISITION OF REFS"
115 LET T=1
120 GOSUB 690
125 PRINT "AFTER ACQUISITION OF REFS"
130 LET E=23.68
135 GOSUB 600
140 PRINT "BEFORE ROLL CALIBRATION"
145 LET T=2.5
150 GOSUB 690
155 PRINT "AFTER ROLL CALIBRATION"
160 LET E=402.63
165 GOSUB 600
170 PRINT "BEFORE CANOPUS ACQUISITION"
175 LET T=3.25
180 GOSUB 690
185 PRINT "AFTER CANOPUS ACQUISITION"
190 LET E=17.76
195 GOSUB 600
201 PRINT "BEFORE FIRST CRUISE"
202 LET T=.25
203 GOSUB 690
210 PRINT "AFTER FIRST CRUISE"
215 LET N=472
220 LET E=.21326
225 LET T=.5
230 GOSUB 700
235 PRINT "AFTER.TURNS, MIDCOURSE AND REACQUISITION"

241 LET E=248.68
245 GOSUB 600
```

Figure F-1. Dual-Mode Hydrazine Basic Language Program

```
50 LET J=4
55 GOSUB 74Ø
6Ø PRINT "AFTER SECOND CRUISE"
65 LET N=96Ø
7Ø LET E=.15581
75 LET T=.5
8Ø GOSUB 7ØØ
85 PRINT"AFTER TURNS,SECOND MIDCOURSE AND REACQUISITION"
9Ø LET E=11.84
295 GOSUB 6ØØ
3ØØ LET J=4
3Ø5 GOSUB 74Ø
31Ø PRINT "AFTER THIRD CRUISE"
315 LET N=816Ø
32Ø LET E=.15581
325 LET T=.5
33Ø GOSUB 7ØØ
335 PRINT "AFTER TURNS,RETRO AND REACQUISITION"
34Ø LET E=6Ø3.94
345 GOSUB 6ØØ
35Ø LET J=4
355 GOSUB 74Ø
36Ø PRINT "AFTER FIRST ORBITAL CRUISE"
365 LET N=24Ø
37Ø LET E=.148Ø4
375 LET T=.5
38Ø GOSUB 7ØØ
385 PRINT "AFTER ORBITAL TRIM, TURNS, ETC."
39Ø LET E=195.42
395 GOSUB 6ØØ
4ØØ LET J=4
4Ø5 GOSUB 74Ø
41Ø PRINT "END OF MISSION"
415 LET N=96ØØ
42Ø LET E=.Ø3516
425 LET T=.5
427 LET J1=7
43Ø GOSUB 7ØØ
435 GO TO 895
5ØØ DATA
5Ø1 DATA
5Ø2 DATA
6ØØ LET J=I
6Ø5 LET V2=V
61Ø LET I2=23Ø
62Ø LET L=E/I2
63Ø LET B2=B2+L
64Ø LET V=V+L/D
65Ø LET X=A/88.Ø1/(A/88.Ø1+B/1Ø.68)
655 LET C1=(1-X)*(1.9872*(2*3.469+3.5Ø83)/3)+X*.169*88.Ø1
657 LET K=C1/(C1-1.9872)
66Ø LET P=P*((V2/V)↑K)
665 GO TO 755
675 LET H1=P*V/Z/1Ø.73/(A/88.Ø1+B/1Ø.68)
685 GO TO 87Ø
69Ø LET J=1ØØ
691 LET N2=Ø
```

Figure F-1 (continued). Dual-Mode Hydrazine Basic Language Program

```
692 LET T=T/J
693 LET N2=N2+1
694 IF N2>JTHEN 865
695 GO TO 730
700 IF P>G THEN 817
702 FOR N1=1 TO N
703 LET L=0
705 LET J=0
710 LET A1=A
715 LET B1=B
720 LET A=A-A*E/I/(A+B)
725 LET B=B-B*E/I/(A1+B)
730 LET B=B+T*R
733 LET B0=B0+T*R
735 LET V=V+T*R/D
740 LET X=A/88.01/(A/88.01+B/10.68)
745 LET I=125.021
755 LET P1=P
771 LET Z=1
775 IF J=1 THEN 675
777 IF J=3 THEN 70
778 IF J=5 THEN 865
780 LET P=(A/88.01+B/10.68)*10.73*H*Z/V
785 LET P2=P
790 LET Y=ABS(P2-P1)/G
795 IF Y>1.0E-5 THEN 755
797 IF J<>4 THEN 800
798 RETURN
800 IF J<>100 THEN 808
802 IFP<G THEN 806
804 PRINT "FULL PRESSURE WAS REACHED AT" N2*T"HRS","P="P
805 GO TO 865
806 GO TO 693
808 IF P>=G THEN 820
810 NEXT N1
815 GO TO 865
817 PRINT "FULL PRESSURE WAS REACHED BEFORE CRUISE","P="P
818 GO TO
820 PRINT "N=" N1,"FULL PRESSURE WAS REACLED AT"N1/2"HOURS CRUISE","P="P
822 LET J=5
823 LET P=G
850 LET V=V+((N-N1)*(E)/(D*I))
855 LET B=10.68*(G*V/(10.73*H))
857 LET B0=B0+((N-N1)*(E/I))
858 GO TO 740
865 LET H1=H
866 LET L=0
867 LET I2=I
870 PRINT "B="B," V=" V,"P="P,"ISP="I2," T="H1
876 IF J=1 THEN 879
877 PRINT"CUMULATIVE LIQUID ELECTROLIZED="B0
878 GO TO 880
879 PRINT"CUMULATIVE LIQUID EXPELLED="B2
880 PRINT
885 RETURN
895 PRINT "TANK"
900 LET R1=(1.05*V/4.189)↑(1/3)
```

Figure F-1 (continued). Dual-Mode Hydrazine Basic Language Program

70172-49D (U)

```
LET T0=2.2*G*R1/320000
LET T1=.001
IF T0>T1 THEN 910
LET T0=T1
PRINT "THICKNESS="T0*12"IN","DIAMETER="2*(R1+T0)"FT"
LET W1=1.1*12.57*((R1+T0/2)↑2)*.161*1728
LET W1=W1*.001
PRINT "6AL-4V TITANIUM TANK WEIGHT="W1"LBS"
PRINT
PRINT "LIQUID EXPELLED AS MONOPROPELLANT="B2"LBS"
PRINT "LIQUID ELECTROLYZED="B0"LBS"
PRINT "TOTAL HYDRAZINE="B0+B2+B3"LBS"
PRINT
PRINT
PRINT "********************NEXT CASE********************"
PRINT
GO TO 4
END
```

Figure F-1 (continued). Dual-Mode Hydrazine BASIC Language **Program**

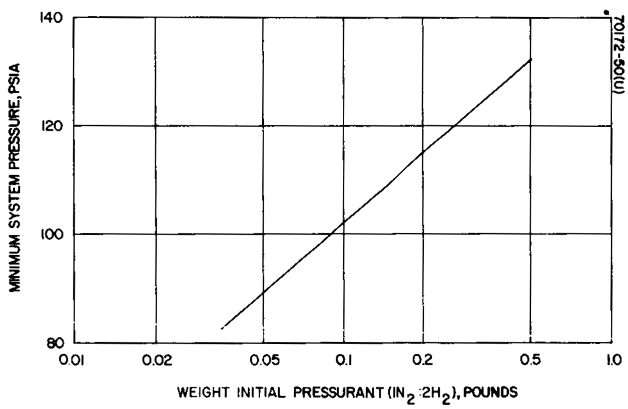


Figure F-2. Dual-Mode Hydrazine System Minimum Pressure Versus Weight of Initial Pressurant at 5 Amperes Peak Current

CATALYTIC ENGINE PERFORMANCE

It is well established that effective specific impulse is lower for catalytic engines of small size. Figure F-3 is a compilation of data from three engine manufacturers. The manufacturers are not identified because of the proprietary nature of the data, but the data points are indicated by three different symbols to show engines of the same make. The solid curve was established by one of the manufacturers from an unspecified number of tests with 0.1, 2.5, 5.0, and 75 lb_f engines at 50 : 1 expansion ratio. Note that the 0.05-pound point on the curve was reported by a different company. It is believed that the solid curve is a fair representation of realizable performance with state-of-the-art engines.

It should be emphasized that only steady-state firings can give the indicated performance. Pulse performance is very much lower and falls off even more rapidly at low thrust levels. For example, a 2-pound engine would be expected to deliver 170 to 180 lb_f-sec/lb_m for 100-millisecond pulses and about 150 to 160 lb_f-sec/lb_m for 50-millisecond pulses. Engines below 0.1 lb_f would be expected to be inferior to cold-gas jets, which may deliver upwards of 120 lb_f-sec/lb_m, the difference being ammonia in the exhaust products and an inefficient chamber design for the catalytic engine compared with a conventional jet thruster.

SYSTEM WEIGHTS

A detailed listing of components and weights appears in Table F-1. In the dual-mode system, appropriate control valves and thruster assemblies are provided for both liquid monopropellant and cold-gas operation. Because retro-level thrust is understood to be acceptable for maneuvering, common engines are used both for maneuvering and roll control during retro. Note, however, that no redundancy has been assumed between liquid and gas thrusters, so complete sets of each type must be included.

POWER REQUIREMENTS

A current range of 3 to 5 amperes has been shown to be suitable for all systems. There are no advantages to higher currents because minimum system pressure remains essentially constant above this range; reduction in current would require an increase in initial pressurant in order to maintain minimum system pressure.

It has been experimentally verified (Hughes, in-house work) that the voltage potential required for reasonable hydrazine electrolysis rates varies from approximately 2 volts for standard mil grade hydrazine (without electrolyte) to 0.5 volt for solutions containing hydrazine nitrate electrolyte. Therefore power could range between 1 and 10 watts, which constitutes a very small demand on the average spacecraft. A theoretical treatment of the continuous amperage requirement follows.

Hydrazine Electrolysis

$$N_2H_4 \xrightarrow[\text{Faradays}]{4} N_2 + 2H_2$$

$$\frac{4 \times 96,500 \text{ ampere-seconds} \times 454 \text{ grams/pound}}{3600 \text{ seconds/hour} \times 32 \text{ grams}} = 1521 \frac{\text{ampere-hours}}{\text{pound}}$$

$$= 1521\ W_p \text{ amperes}$$

average (continuous)

W_p = 9.1 pounds liquid electrolyzed (worst case from Table E-2)

T = 9720 hours (mission duration)

$I = \frac{1521 \times 9.1}{9720} = 1.43$ amperes continuous over the total mission

Dual-Mode Hydrazine System

W_p = 14.29 pounds (all cases)

T = 9720 hours

$I = \frac{1521 \times 14.29}{9720}$ - 2.24 amperes continuous over the total mission

Water Electrolysis

$$2H_2O \xrightarrow[\text{Faradays}]{2} 2H_2 + O_2$$

$$\frac{2 \times 96,500 \times 454}{3600 \times 18} = 1352 \frac{W_p}{T} \text{ amperes average}$$

W_p = 10.3 pounds liquid electrolyzed (worst case from Table E-1)

T = 9720

$I = \frac{1352 \times 10.3}{9720} = 1.44$ amperes continuous over the total mission

In actual system operation, continuous power is not used because of variable propellant consumption. The preferred method is a pressure switch which terminates power to the cell when full system pressure is reached. A somewhat higher peak power is therefore required during periods of cell operation.

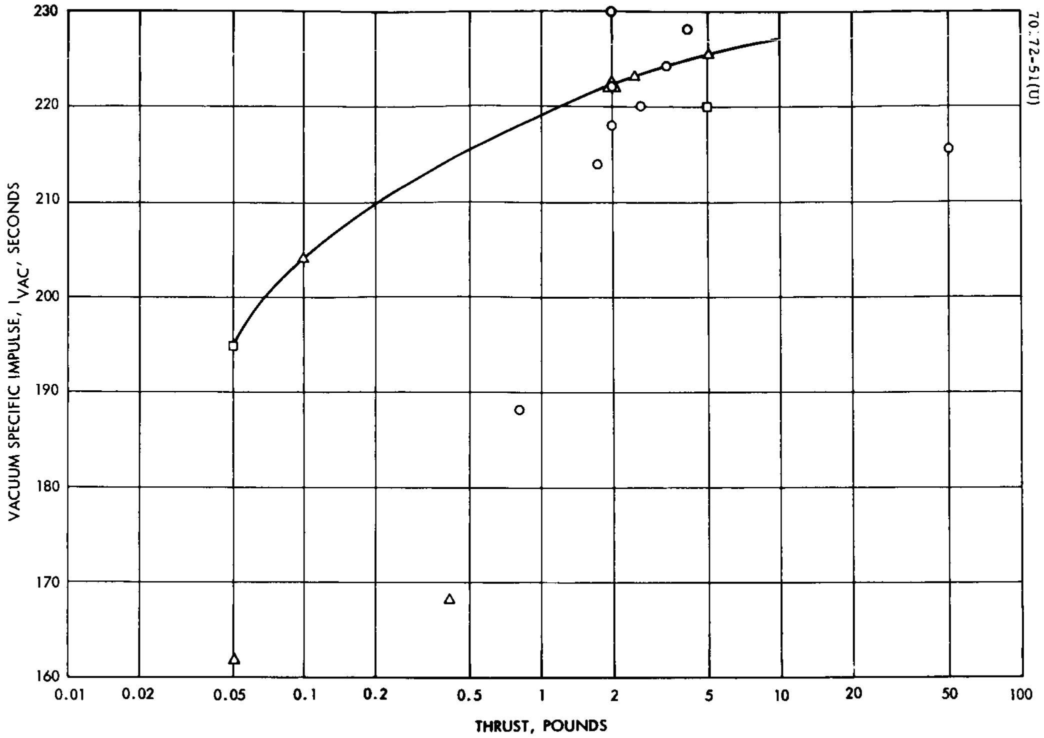


Figure F-3. Specific Impulse Versus Thrust for Various Engines

TABLE F-1. DUAL-MODE HYDRAZINE SYSTEM COMPONENTS AND WEIGHTS

(Number of components required in parentheses, weight in pounds)

Component	Unit Weight	1a	1b	1c	1d	2a	2b	2c	2d	3a	3b	4a	4b	4c	4d
Fill valve	1.3	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(2) 2.6	(1) 1.3	(1) 1.3	(1) 1.3	(1) 1.3
Control valve															
Gas	0.4	(48)19.2	(48)19.2	(48)19.2	(48)19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Liquid	0.4	(48)19.2	(48)19.2	(48)19.2	(48)19.2	(48) 19.2	(48) 19.2	(48) 19.2	(48) 19.2	(12) 4.8	(12) 4.8	(24) 9.6	(24) 9.6	(24) 9.6	(24) 9.6
Regulator (weight/ relief valve)	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—
Pressure switch	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Pressure transducer	0.25	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(2) 0.5	(1) 0.25	(1) 0.25	(1) 0.25	(1) 0.25
Filter	0.5	(2) 1.0	(2) 1.0	(2; 1.0	(2) 1.0	(4) 2.0	(4) 2.0	(4) 2.0	(4) 2.0	(4) 2.0	(4) 2.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0
Thruster															
0.08 lb_f gas	0.06	(12) 0.72	—	(12) 0.72	—	(12) 0.72	—	(12) 0.72	—	(12) 0.72	(12) 0.72	(6) 0.4	—	(6) 0.4	—
0.04 lb_f gas	0.06	—	(24) 1.44	—	(24) 1.44	—	(24) 1.44	—	(24) 1.44	—	—	—	(12) 0.7	—	(12) 0.7
0.83 lb_f liquid	0.5	—	(24)12.0	—	(24)12.0	—	(24) 12.0	—	(24) 12.0	—	—	—	(12) 6.0	—	(12) 6.0
1.67 lb_f liquid	0.5	(12) 6.0	—	(12) 6.0	—	(12) 6.0	—	(12) 6.0	—	(12) 6.0	(12) 0.6	(6) 3.0	—	(6) 3.0	—
Electrolysis cell	0.5	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(2) 1.0	(1) 0.5	(1) 0.5	(1) 0.5	(1) 0.5
Zero-g device	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Manifolds and bracketry	5.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(2) 10.0	(1) 5.0	(1) 5.0	(1) 5.0	(1) 5.0
Totals															
Fixed weights		58.4	65.1	58.4	65.1	71.7	78.4	71.7	78.4	42.9	42.9	35.9	39.6	35.9	39.6
Propellant		22.6	22.6	31.9	31.9	45.2	45.2	63.8	63.8	67.9	95.8	22.6	22.6	31.9	31.9
Pressurant		0.015	0.015	0.05	0.05	0.03	0.03	0.1	0.1	0.02	0.08	0.015	0.015	0.05	0.05
Tankage		0.85	0.85	1.2	1.2	1.7	1.7	2.4	2.4	2.16	2.9	0.85	0.85	1.2	1.2
Total system weight		81.9	88.6	91.6	98.3	118.7	125.4	138	144.7	113	141.4	59.4	63.1	69.0	72.7

APPENDIX G. FILTERS, VALVE LEAKAGE, AND MATERIALS

FILTRATION REQUIREMENTS

Since prevention of valve leakage or function impairment is directly responsible for the filtration requirement, a brief discussion of leak phenomena is in order.

Gas leakage across a valve hard seat is a result of the mating surfaces being too far apart in relation to the average distance a gas molecule must travel in crossing the seat surface. Three situations occur that may keep the mating surfaces apart:

1) Cuts or scratches across the seat (These are normally caught in inspection and will not be discussed further.)

2) Surface asperities

3) Dirt

The more the asperities can be reduced in manufacture, the better the valve will seal. On a clean seat, the largest asperities of the seat and poppet make first contact. As seating pressure increases, the larger asperities are deformed, more and more smaller asperities come into contact, and seating pressure finally is balanced without further deformation. A gas molecule then must travel a long and complicated path between the asperities to reach the downstream side. Within a few thousandths of an inch, the gas flow becomes molecular (molecules striking the sides of the leakage channel more frequently than striking each other). This means that although the gas has not been stopped from escaping, the flow has been reduced several orders of magnitude below that achieved when the largest asperities first contacted each other.

The process of deforming the surface metal on the seat by increasing the loading may be traced by measuring the leakage. In Figure G-1 leakage versus apparent contact stress is plotted. This stress is computed by dividing the area by the load. On the microscopic level there are a great many void spots bearing no load, but this fact was not considered when calculating the stress as indicated by the word "apparent".

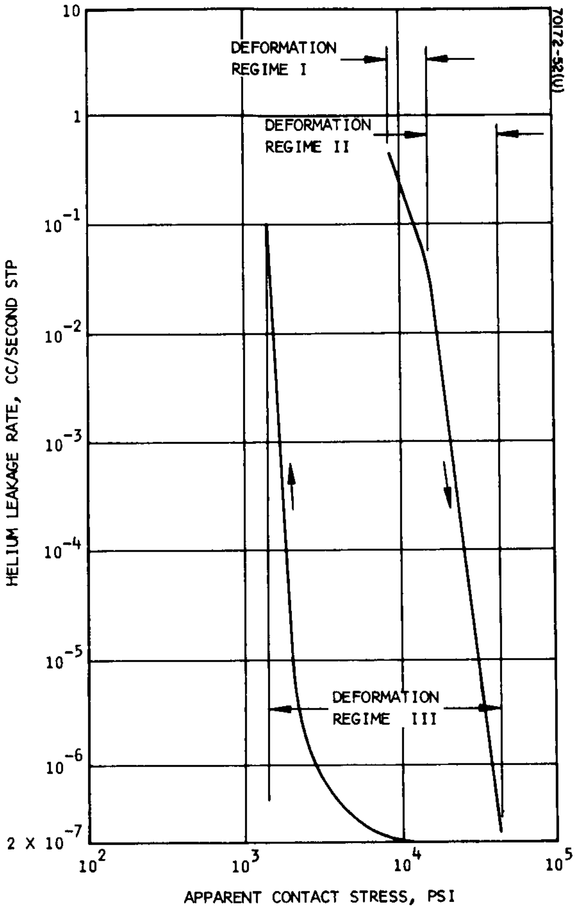


Figure G-1. Leak Rate Versus Stress for Flange Seal

The regimes shown in Figure G-1 describe the type of deformation the metal undergoes. In regime I, most of the asperities are forced into contact with only light deformation of the larger peaks. During regime II, the asperities are crushed down and there is significant lateral movement as the metal flows into void areas.

When the load is reversed after passing through regime II, the conditions of regime III are obtained because, from flattening of asperities, the surfaces are more nearly smooth. As a result, less load is needed to maintain intimate contact and low leakage. Once separation begins, however, it is more sudden and complete.

As might be expected, the regime III curve is nearly reversible. This is very significant since it means that low leak rates can be achieved by hard seat valves with fairly low seating loads. The only requirement is that the seating surfaces must be deformed through regime II. Practically speaking, this means that during the assembly process a temporary heavy load is applied to the seating surfaces. This flattens and smoothes the surfaces by deforming the asperities. The heavy load may then be replaced by a light closing spring, and low leakage is thus maintained.

The process just described is very much like the one used at Hughes to achieve good valve seating in its attitude control valves. At Hughes the surfaces are first gold plated. Gold asperities are, of course, much easier to deform and therefore a smoother surface is produced.

The final cause of seat leakage – dirt – is an obvious but complicated problem. Several formulas are available for calculating gas flow rate through a narrow gap, the differences among them being slight. All depend on the assumption that the gas flow is in the molecular region. Calculations can be made for maximum tolerable particle size, but the simplifications necessitated by this approach make it valuable only as a rough guide. A calculated gap width for valve designs and leak rates of interest to the present mission would be of the order of 300 microinches. This corresponds to allowable contaminant particles less than 8 microns in diameter.

Several questions are still unanswered, however. How many 8-micron particles can the seat tolerate? Will larger particles be flattened to acceptable sizes? What is the chance that the particle will fill a void and actually improve the leak rate, or will the particle be blown off the seat during the next actuation? Apparently little or no information is available in this regard.

In view of the uncertainties, selection of a filter of the proper pore size is not a clear-cut process. Good results were obtained on Surveyor with 15-micron screen filters, but in view of the large number of operating cycles required on this mission, a 10-micron filter is recommended. No particular style filter is recommended, because the effectiveness of a given filter is often a function of the manufacturer.

MATERIALS

Some materials suitable for valve parts operating with various working fluids are listed in Table G-1.

TABLE G-1. MATERIALS FOR VALVES AND OTHER COMPONENTS

System	Housings and Lines	Valve Poppets	Valve Seat	Elastomeric Seals	Fluid and Condition
Nitrogen	Titanium; 300 series stainless steel; aluminum	Nylon; 300 series stainless steel; tungsten carbide	Gold plated stainless; soft aluminum	Nylon; Viton; silicone; buna N	N_2 gas 70°F (95 percent) -100°F at end of roll control
Ammonia	Aluminum, 300 series stainless steel	300 series stainless steel; tungsten carbide	Gold plated stainless; soft aluminum	Viton; silicone	NH_3 gas 20 to 90°F
Hydrazine (hot gas)	Ti-6AL-4V; 300 series stainless steel; 5052, 6061 Al	300 series stainless steel; boron nitride	300 series stainless steel	Teflon (low temperature only); ethylene propylene	N_2, H_2 gas <1 percent, N_2H_4 up to 600°F
Water electrolysis	300 series stainless steel; aluminum	300 series stainless steel; tungsten carbide	Gold plated stainless; soft aluminum	Nylon; Viton; silicone; buna N	H_2, O_2 gas 70°F
Hydrazine electrolysis	5052, 6061 Al; 300 series stainless steel	300 series stainless steel tungsten carbide	Gold plated stainless; soft aluminum	Teflon; ethylene propylene	N_2, H_2 gas 70°F

APPENDIX H. THERMAL CONTROL

Substantial differences in the requirements for thermal control exist among the attitude control systems considered here. In general, the least demanding is the cold-gas system, as exemplified by nitrogen. Liquid-containing systems will demand more stringent thermal control because of the possibility, arising from the rapid change of vapor pressure with temperature, that vapor will condense in portions of the system which must be free of liquid or solid to function properly.

It is assumed for the purposes of this discussion that the attitude control systems can be divided into two groups:

- The components located at the periphery of the spacecraft such as thrusters, valves, and the lines leading to these components.
- A central group of components, such as the tanks, regulators, and plenums which have relatively high heat contents and can be located in a thermostatically controlled compartment, if desired. The hydrazine catalytic plenum, which may reach very high temperatures, is a special case (see following discussion).

PERIPHERAL COMPONENTS

Passive Thermal Control

Passive thermal control of the thrusters, thruster valves, and lines is made difficult by the low heat capacities of these components, thermal coupling of the attitude control system with the remaining spacecraft structure, and the fact that the solar thermal flux varies with distance from the sun. (At the distance of Mars, it is approximately half the flux at the distance of Earth.) The problems of a passive thermal control method are further compounded if the vehicle alters its orientation with respect to the sun for any appreciable length of time during maneuvers, or acquires an orbit that takes the vehicle through the shadow of a planet. The relatively rapid decrease in temperature of the peripheral components under these conditions could place the spacecraft in jeopardy of permanently losing all attitude control within a short period of time.

While these arguments are necessarily qualitative in the absence of detailed knowledge concerning the spacecraft geometry and trajectory, it is believed that they are sufficient to indicate that electrical heaters or other sources of heat are required to guarantee proper operation of the attitude control systems considered here, with the possible exception of the systems employing cold gases which liquify only at cryogenic temperatures.

Active Thermal Control

The simplest active thermal control method for the peripheral components is a continuously operating trickle heater supplying enough heat to maintain the temperature of the peripheral components at a temperature higher than the highest of any of the central components. The simplest method of heating is by means of a small continuous current through the solenoids of the thruster valves. It is likely that heating of the lines would be unnecessary if they were well insulated with superinsulation, and heat flow to the spacecraft frame were minimized by adequate thermal standoffs.

This type of heating would guarantee that any distillation occurring in the system would move liquids toward the tankage. Such an arrangement would be dynamically stable in operation, in that:

1) Any liquid finding its way into the neighborhood of the thruster valves would be removed by distillation since the thruster valves would be the warmest points in the systems.

2) Excessive regulator leakage in the hydrazine electrolysis, water electrolysis, vaporizing liquid, and perhaps the hydrazine catalytic plenum system would not fill the lines with liquid or solid material.

This heating method has the incidental advantage of improving the pulse performance of the thrusters somewhat because of the higher temperatures of the propellant gas in the line immediately upstream of the thruster valve.

CENTRAL COMPONENTS

In general the attitude control systems considered here will require some form of thermostatic control for the propellant storage tanks and other central components. Passive thermal control methods might be employed for cold-gas systems and for ammonia systems requiring external heat sources for supplying the heat of vaporization.

In cases of the systems containing hydrazine or water, the relatively high freezing points of hydrazine and water (35 and 32°F respectively), the time limitations of nonsun-oriented maneuvers and the necessity of keeping the tank temperature at or below the temperature of the peripheral components at all times would make passive thermal control difficult for all systems except the cold-gas system.

One possible approach to the problem of thermal control of the tankage and other central components of the liquid-containing systems (except for the plenum of the hydrazine catalytic plenum system) might be integration with the thermal control of the electronics compartment. The heat required to maintain the temperature of the attitude control system components is likely to represent only a minor portion of the heat being dumped to space by the thermal control system of the large vehicle involved here. The required temperature control might therefore incur no power penalty and very little weight penalty.

If thermal coupling of the electronics compartment and the central components of the attitude control system were undesirable because of high transient temperature changes in the attitude control system, the required heat could be obtained by a small electrical heater. Temperature regulation could be accomplished by a thermoswitch or by louvers on the compartment.

Plenum of Hydrazine Catalytic Plenum System

During periods of steady-state operation the surface of the hydrazine catalytic plenum will become quite hot, possibly approaching 1200° F. This component must therefore be thermally isolated from the hydrazine tankage and from temperature-sensitive components of the space vehicle. It is presumed that the hydrazine employed may contain small quantities of water as an impurity; the plenum and the lines to the thrusters will therefore probably require active thermal control to prevent occurrence of a cold spot in the lines where water vapor might condense and freeze.

A method for thermally insulating the hydrazine catalytic system plenum is not immediately apparent, and should receive further consideration. The conventional types of superinsulations employ plastic fibers which would melt or decompose at the plenum maximum temperature. Heat shielding with metal foil layers would be both heavier and less efficient, as would the construction of a separate louvered compartment for the plenum. Insulation of the lines to the thrusters may also present some problems due to the high temperature which the lines may occasionally reach.*

CONCLUSIONS

The types of thermal controls necessary for the systems considered here are outlined in Table H-1.

*See "Voyager Spacecraft System, Vol. A., Preferred Design for Flight Spacecraft and Hardware Subsystems", NASA CR-71513, Boeing Company, January 1966.

TABLE H-1. THERMAL CONTROL

System	Tanks and Central Components	Peripheral Components	Condensable Species	Condensation Temperature, °F (a)	Minimum Allowable Temperature, °F (b)
Cold gas (nitrogen)	Probably passive	Probably passive	N_2	-296	-296
Vaporizing liquid (Ammonia) (Propane)	Probably active	Probably active	NH_3 Propane	18 14	18 14
Hydrazine catalytic Plenum	Active (plenum is special case)	Probably active	H_2O NH_3	102 -62	32
Hydrazine electrolysis Plenum	Active	Active	N_2H_4	32	35
Water electrolysis Plenum	Active	Active	H_2O	32	32
Hydrazine dual system	Active	Active	N_2H_4	32	35

(a) Estimated temperature at which condensation first appears at a total gas pressure of 50 psia. It is assumed that the sources of the gases are:

- Saturated gases at 70°F and 200 psia for hydrazine electrolysis and water electrolysis cells.
- A gas mixture containing 2 percent H_2O and 10 percent NH_3 (by volume) at 50 psia in the case of the hydrazine plenum system.

It should be noted that the condensation of liquid in the system does not necessarily imply system failure. For vaporizing liquid or cold gas systems, the performance losses might be significant if liquid were allowed to condense in some portion of the line. For other systems, entrainment of liquid in the gas stream would probably result in relatively small perturbations in performance.

(b) Estimated minimum allowable temperature for the system is:

- The condensation temperature for the cold gas and the vaporizing liquid systems, and
- The freezing point of potential condensed materials in the lines of the other systems.
- In most cases the active thermal control requirements would not be expected to consume more than a few watts of power per line or per component in the central group of components.
- Further analysis of the system will depend upon the geometry of the spacecraft, the types of insulations chosen, and a more detailed knowledge of the individual systems.

APPENDIX I. PROPELLANT PERFORMANCE

Performance predictions for low-thrust cold-gas nozzles are subject to uncertainties which are generally insignificant in large combustion rockets. Condensation, boundary layer, and real-gas effects may come into play, and the quantitative effects of these phenomena on specific impulse are not entirely resolved, either theoretically and experimentally.

As an in-house effort, Hughes has undertaken to develop a computer program capable of treating real-gas nozzle expansion processes. The indications to date are that real-gas effects will be of small importance in the present study. The program is described in this section. A simplified treatment, involving only condensation and boundary layer corrections, was employed for most of the calculations.

ISENTROPIC NOZZLE EXPANSION OF AN IDEAL GAS

The familiar equations (Reference 1) for isentropic nozzle expansion:

$$\frac{1}{\epsilon} = \left(\frac{\gamma + 1}{2}\right)^{1/\gamma - 1} \left(\frac{P_e}{P_c}\right)^{1/\gamma} \sqrt{\frac{\gamma + 1}{\gamma - 1}\left[1 - \left(\frac{P_e}{P_c}\right)^{\frac{\gamma - 1}{\gamma}}\right]}$$

$$I_{vac} = \frac{1}{g} \sqrt{\frac{2g\gamma R T_c}{(\gamma - 1)}\ 1 - \left(\frac{P_e}{P_c}\right)^{\frac{\gamma - 1}{\gamma}}} + \frac{144\ P_e \epsilon}{\rho_t u_t}$$

where

I_{vac} ≡ vacuum specific impulse, seconds

g ≡ gravitational constant, ft/sec^2

γ ≡ specific heat ratio

$R \equiv$ gas constant, (1544 ft lb/lbmol, °R) molecular weight

$T_c \equiv$ chamber temperature, °R

$P_e \equiv$ exhaust plane pressure, psia

$P_c \equiv$ chamber pressure, psia

$\epsilon \equiv$ area ratio

$\rho_t \equiv$ density at the throat, lb/ft^3

$u_t \equiv$ velocity at the throat, $\sqrt{\frac{2gRT_c\gamma}{M(\gamma + 1)}}$, ft/sec

were employed to determine the specific impulse of noncondensing cold-gas mixtures assumed ideal. Some results are given in Table I-1.

TABLE I-1. THEORETICAL SPECIFIC IMPULSE FOR SEVERAL COMPOSITIONS

(Area ratio, ϵ = 100. No condensation or boundary layer corrections.)

Gas	Mole Fraction (X)	Chamber Temperature, °F	I_{vac}, seconds
H_2	1	75	288.4
N_2	1	62	76.4
$N_2 + 2H_2$	1	75	125.3
NH_3	1	62	104.6
CF_4 (Freon 14)	1	75	53.6
CHF_3 (Freon 23)	1	75	58.2
Cyclic C_4F_8 (Freon C318)	1	75	39.7
$XCF_4 + (1-X)(O_2 + 2H_2)$	0.5	62	63.6
$XCF_4 + (1-X)(N_2 + 2H_2)$	0	75	125.3
	0.2	75	82.8
	0.5	75	63.8
	0.8	75	56.2
	1.0	75	53.6

EQUILIBRIUM EXPANSION OF CONDENSABLE VAPOR

Calculation of Point and Extent of Condensation

The results of Table I-1 are predicated on the persistence of the ideal gaseous state throughout the expansion; in actuality, condensation may occur in some cases. The effect of condensation may be bounded by the assumption that at each point in the nozzle an amount of condensed phase corresponding to the equilibrium quantity at the local conditions is formed.* We have followed the development of Altman and Carter (Reference 3). The first step is to locate the point of condensation as the point in the expansion at which the isentrope intersects the vapor pressure curve. For example, the result for nitrogen is shown in Figure I-1. Vapor pressure data for nitrogen were taken from Reference 4. The enthalpy drop from the chamber to this point was computed from the specific impulse, derived from the relationship

$$\Delta H = 1/2\ m u_o^2$$

where

$\Delta H \equiv$ enthalpy drop for mass m

$u_o \equiv$ velocity at point of condensation.

Further points down the nozzle were then treated by selecting a pressure, determining the temperature from the vapor pressure curve, and, from specific heat and heat of vaporization data (Reference 6), computing the local mole fraction of liquid, X_ℓ:

$$X_\ell = \frac{R \ln \frac{P_o}{P} + C_P \ln \frac{T_o}{T}}{\Delta H_V / T}$$

where

$X_\ell \equiv$ mole fraction of liquid

$R \equiv$ gas constant 1.9872 calories/gmole °K

$P_o \equiv$ pressure at onset of condensation, Atm

$P \equiv$ pressure at point of intersect, Atm

$C_P \equiv$ specific heat – calories/gmole °K

$T_o \equiv$ temperature at onset of condensation, °K

*Thermal and velocity equilibrium between gaseous and condensed species is assumed.

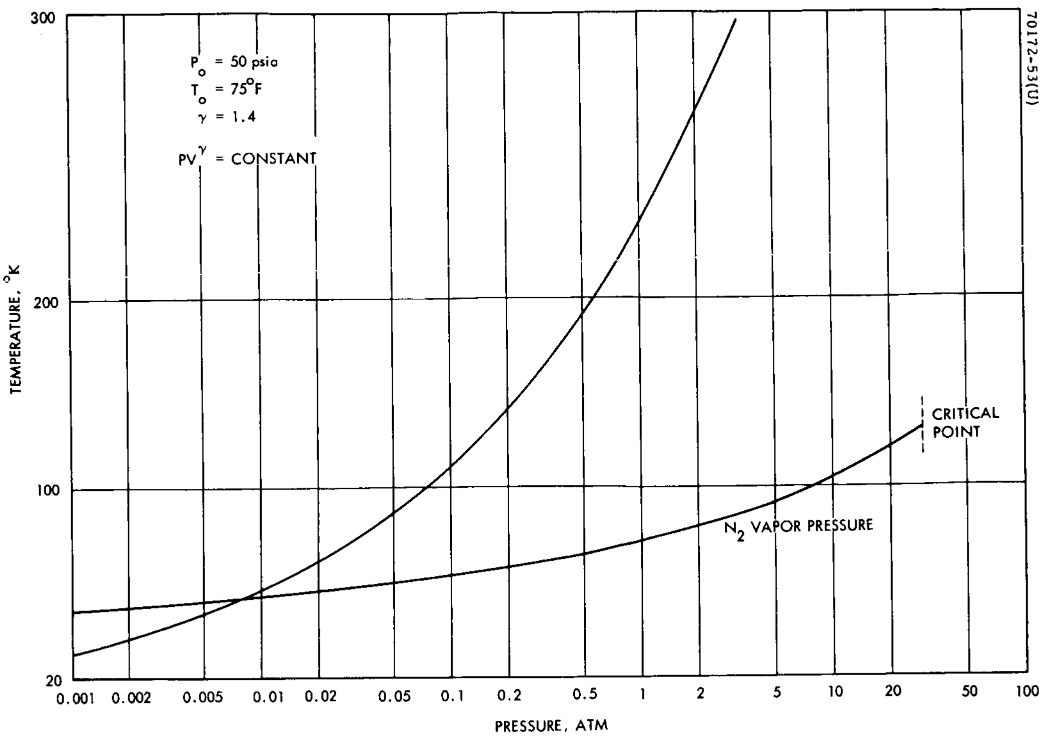
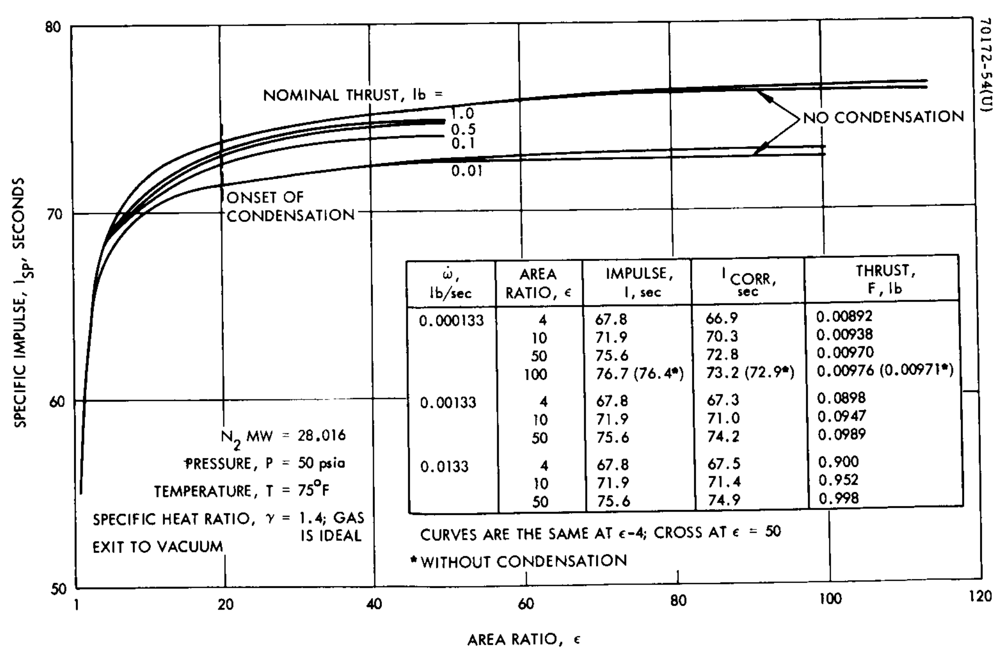


Figure I-1. Equilibrium Expansion of Nitrogen



$\dot{\omega}$, lb/sec	AREA RATIO, ϵ	IMPULSE, I, sec	I_{CORR}, sec	THRUST, F, lb
0.000133	4	67.8	66.9	0.00892
	10	71.9	70.3	0.00938
	50	75.6	72.8	0.00970
	100	76.7 (76.4*)	73.2 (72.9*)	0.00976 (0.00971*)
0.00133	4	67.8	67.3	0.0898
	10	71.9	71.0	0.0947
	50	75.6	74.2	0.0989
0.0133	4	67.8	67.5	0.900
	10	71.9	71.4	0.952
	50	75.6	74.9	0.998

Figure I-2. Nitrogen Thruster Specific Impulse Versus Area Ratio With Condensation and Boundary Layer Effects

$T \equiv$ temperature at point of intersection, °K

$\Delta H_V \equiv$ heat of vaporization — calories/gmole

Knowledge of X_ℓ then permitted determination of the total enthalpy change from the chamber. The corresponding area ratio was then found from continuity.

BOUNDARY LAYER CORRECTIONS

In small nozzles boundary layer growth may lead to a significant decrement in I_{sp}. The losses were estimated by the procedure of Spitz, et al (Reference 10) in which the loss in thrust coefficient, relative to the one-dimensional isentropic value, is predicted in terms of the throat Reynolds number. The effects reported in Reference 10 for small hydrogen thrusters indicated that the cumulative viscous losses with increasing area ratio may lead to relatively low optimum area ratios in low-thrust devices.

Throat Reynolds number data for nitrogen were computed assuming an ideal gas and using viscosity data from the critical tables (Reference 6); the results are given in Table I-2. The formal procedure in Reference 10 is to assume a specific impulse and calculate the flow rate for a given thrust. The throat diameter for isentropic flow and the throat Reynolds number are then computed, to yield a loss in thrust coefficient for a given area ratio. A new specific impulse may then be calculated, and the process iterated, but this has proved to be unnecessary. The procedure is in any event approximate, and does not consider condensation effects. It may be noted that Greer (Reference 11) has recently questioned the validity of the correlation.

RESULTS

Results for nitrogen and ammonia are shown in Figures I-2 through I-6. It will be seen (Figures I-3 and I-5) that even for ammonia, the specific impulse is only slightly different for real and ideal gas calculations.*

The condensation effects are especially interesting. Less than 1 second difference in I_{sp} is observed in Figure I-2 at high area ratio for nitrogen with and without condensation. For ammonia, however, the effect may be on the order of 15 percent (Figures I-3 through I-6). The significant point is that an increase in specific impulse on condensation is indicated. Differing

*It may be noted that ideal gas behavior does not imply constant gamma. For hydrogen, for instance, the loss of a degree of freedom between 300° and 50°K leads to an error of 11 seconds in I_{sp} if the initial specific heat ratio is assumed constant out to $\epsilon = 50$.

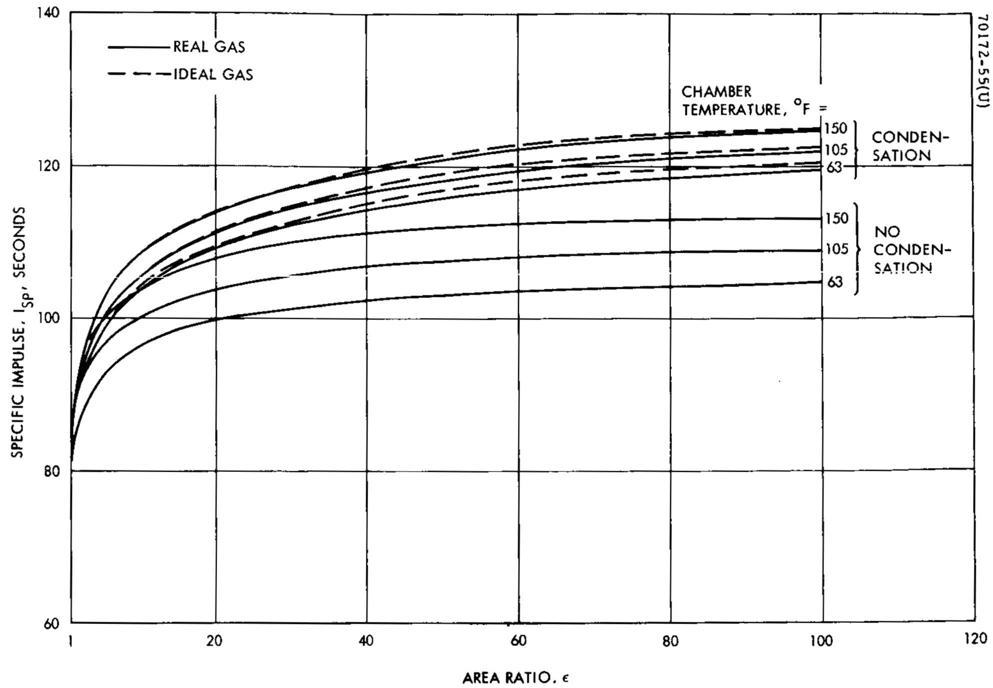


Figure I-3. Ammonia Vaporjet Specific Impulse at 50 psia Chamber Pressure

Exit to vacuum

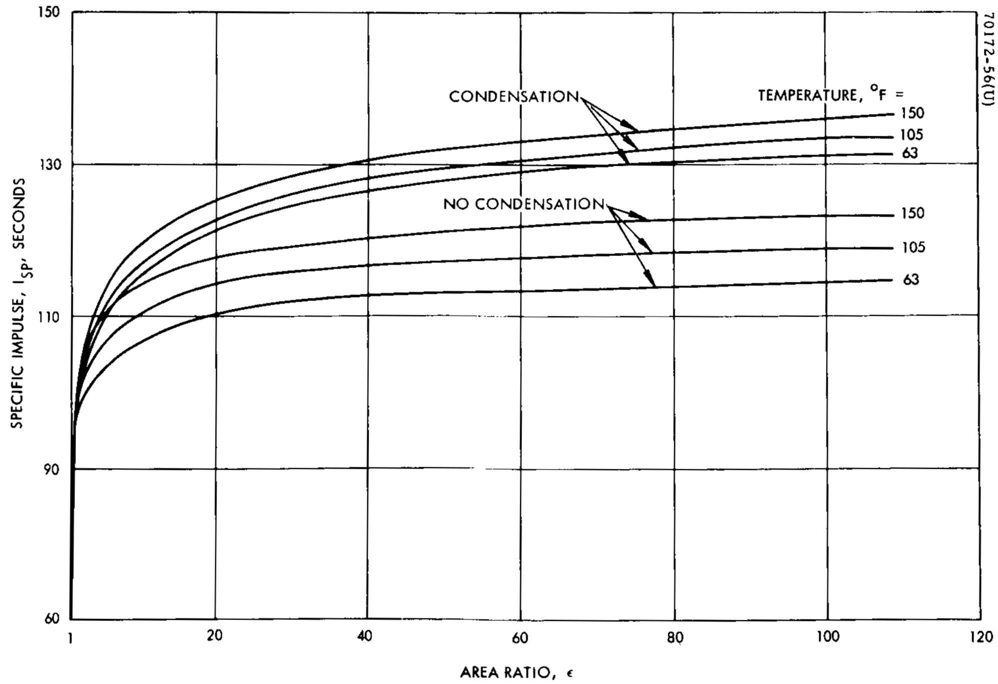


Figure I-4. Ammonia Vaporjet Specific Impulse at 100 psia Chamber Pressure

Exit to vacuum ideal gas

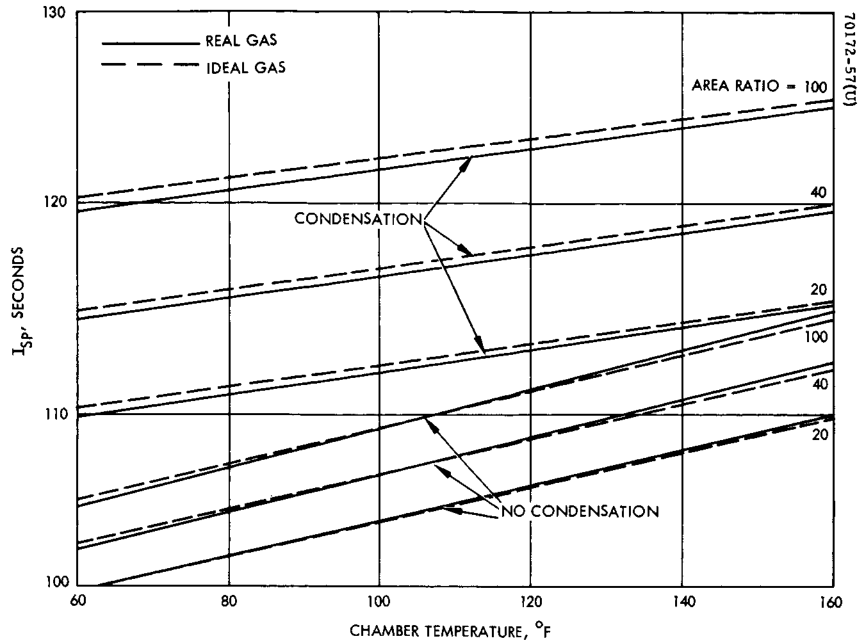


Figure I-5. Ammonia Vaporjet Specific Impulse as Function of Chamber Temperature at 50 psia Chamber Pressure

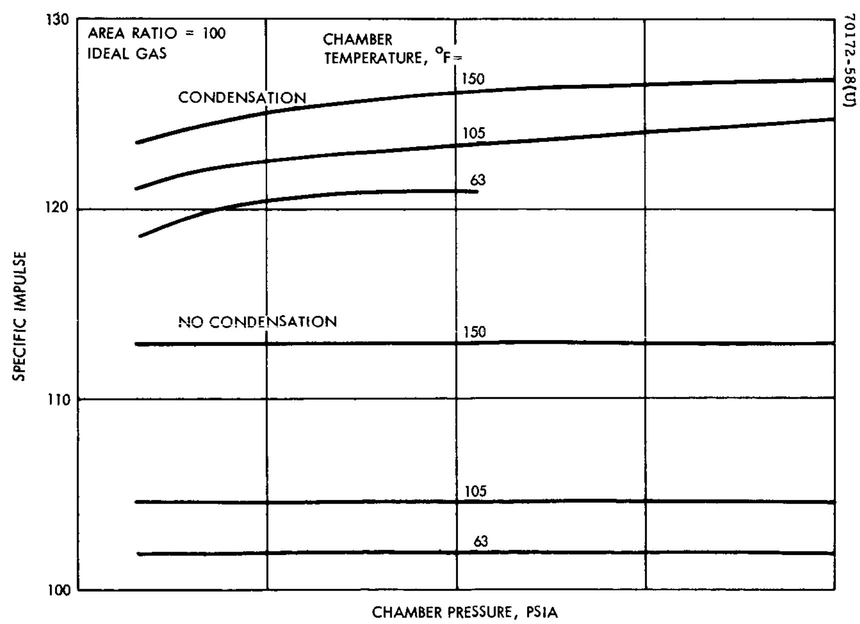


Figure I-6. Ammonia Vaporjet Specific Impulse as Function of Chamber Pressure and Temperature

TABLE I-2. REYNOLDS NUMBER AND NOZZLE THROAT SIZE FOR NITROGEN

(T_c = 75°F, P_c = 50 psia)

F, pound	d, cm	R_e
0.01	0.0313	1.67×10^4
0.1	0.0989	5.30×10^4
0.5	0.222	1.19×10^5
1.0	0.313	1.67×10^5

views on the direction of this effect have been presented (References 7 through 9), which is not surprising in view of the opposite effects on condensation of heat release and reduction in the amount of working gas. The limited experimental data available indicate that the theoretical increase is not realized in practice, because of the short nozzle residence times.

Boundary layer effects are not large for the size thrusters of this study. A brief examination of the heat (Reference 10) indicates that the losses for ammonia would be of the same order as those shown for nitrogen in Figure I-2.

REAL-GAS NOZZLE EXPANSION PROGRAM

Under an independent research and development program, Hughes has developed a real-gas treatment of cold-gas nozzle performance. The Beattie-Bridgeman equation of state, which contains compressibility data, is employed. Both single- and multi-component systems are treated; in the latter case, the entropy of mixing and the Dalton's law equations are formulated in terms of ideal gases for simplicity. Because the pressures are low in the regions at which these terms become important, it is felt that any corrections for real gas effects on these equations would be nominal. For one-component systems such as those described in this report, these approximations are not, of course, involved.

The computer program calculates propellant properties along a nozzle on the assumption of one-dimensional freestream flow. Boundary layer corrections can be superimposed on the initially calculated conditions. The gas is assumed to be inviscid but capable of accelerating any condensate which may appear. The entire process is adiabatic.

Two options are available in the computation: supercooling with no condensation, and equilibrium condensation. The true case will be intermediate between these extremes, but cannot easily be calculated accurately because the kinetics of nucleation are poorly understood.

EQUATIONS AND LOGIC

Pressure, temperature, composition, and velocity of the initial chamber mixture are specified. The vapor pressure of each component and the specific heat of the mixture are calculated from the following empirical equations:

$$\ln p_i = Q_i + D_i/T + S_i \ln T + N_i T$$

$$C_p(p^*) = \alpha + \beta T + \phi T^2 + \varphi T^{-2}$$

where p^* is the base pressure at which specific heat is measured. The constants α, β, ϕ, and φ are the mole fraction averages of the individual constants and must be adjusted with each step during condensation since the relative composition of the vapor may be changing. The equation of state used is the Beattie-Bridgeman equation:

$$pV^2 = RT\left[V + B\left(1 - \frac{b}{V}\right)\right]\left[1 - \frac{C}{VT^3}\right] - A\left(1 - \frac{a}{V}\right)$$

where again the constants must be properly averaged at all times. From this equation of state, the various compressibility relations are derived by performing the indicated partial differentiations:

$$\left(\frac{\partial V}{\partial T}\right)_p, \left(\frac{\partial^2 V}{\partial T^2}\right)_p, \left(\frac{\partial V}{\partial p}\right)_T$$

The program begins by calculating the specific heat of the gas (or vapor) at the temperature and pressure at which it exists:

$$C_p = C_p(p^*) + \int_{p^*}^{p} \frac{T}{J}\left(\frac{\partial^2 V}{\partial T^2}\right)_p dp$$

For a reversible adiabatic expansion with external work, the total entropy of the fluid remains unchanged. For a gas,

$$dS = \left(\frac{\partial S}{\partial T}\right)_{P M_i} dT + \left(\frac{\partial S}{\partial P}\right)_{T, M_i} dP + \left(\frac{\partial S}{\partial M_j}\right)_{P, T, M_j \neq M_k} dmj$$

$$dS = \frac{C_p}{T} dT - \frac{1}{J}\left(\frac{\partial V}{\partial T}\right)_p dP - \frac{R}{J} \Sigma_j dx_j (\ln X_j + 1)$$

If the kinetics of the situation do not allow condensation of a supersaturated vapor, then only the gas phase will be present. In this case, composition cannot change, so that

$$0 = \frac{C_p}{T} dT - \frac{1}{J}\left(\frac{\partial V}{\partial T}\right)_p dP,$$

yielding

$$\frac{dP}{dT} = \frac{C_{P/T}}{\frac{1}{J}\left(\frac{\partial V}{\partial T}\right)_p}$$

which tells how to step temperature with pressure down the nozzle.

The other extreme possible is to consider equilibrium condensation. In this case the composition of the vapor can change. Until the onset of condensation, pressure should be stepped with temperature in the same fashion as the no-condensation case. A test for condensation is done by checking the relationship between the partial pressure and the vapor pressure at the particular nozzle station:

$Y_i P = P_i$ (T) condensation occurs in species i

$Y_i P < P_i$ (T) no condensation is possible in species i

When condensation may occur, the heat of vaporization may be defined from the Clausius Clapeyron equation:

$$\Lambda_i = \frac{P_i T}{J}\left[V_i - V_2 \frac{S_i}{T} - \frac{D_i}{T^2} + N_i\right]$$

The change in composition (of the condensing species) of the vapor may then be calculated by differentiating the condition of condensation,

$$P_i = Y_i P$$

with respect to temperature and substituting dP_i/dT from the Clausius-Clapeyron equation to yield

$$\frac{dY_i}{dT} = \frac{1}{P}\left[\frac{J\Lambda_i}{T(V_i - V_2)} - Y_i \frac{dP}{dT}\right]$$

In a nozzle, condensation may be approximated by an isothermal process in each nozzle increment, but expansion still occurs. For the gas phase,

$$dS = \frac{C_p}{T} dT - \frac{1}{J}\left(\frac{\partial V}{\partial T}\right)_p dP - \frac{R}{J} \sum_{j \text{ all species}} dX_j \left(\ln X_j + 1\right)$$

This same equation applies to a liquid; however, liquids may be assumed to be incompressible in general and mixing may be minimal so that

$$dS = \frac{C_\ell}{T} dT$$

An isothermal condensation of a saturated vapor is derivable from the Clausius-Clapeyron equation and a Maxwell relationship, to yield the entropy change for this phase change (with the approximation that the liquid volume is small compared with the gas volume):

$$dS = \frac{1}{T} \sum_{\substack{i \\ \text{saturated} \\ \text{species}}} \Lambda_i \, dy_i$$

Let L be the number of moles of liquid per mole of gas initially present. Then the amount of condensation per nozzle increment is

$$dL = -(1 - L) \sum_i dyi$$

where the summation is over the saturated species only. New mole fractions are calculated with each change in composition.

The equilibrium condensation process including the expansion is assumed to be isentropic in the free stream:

$$dS = 0 = (1 - L)\, dS_{gas} + (1 - L)\, dS_{condensation} + L\, dS_{liquid}$$

Substituting and rearranging,

$$0 = \frac{L}{1 - L} \frac{C_{\ell}}{T} dt + \sum_{i} dx_j (\ln X_j + 1)_{\text{all species}}$$

dividing by dT and substituting for dy_i/dT yields, on solution,

$$dT = \frac{dP \left[\frac{1}{P} \sum \frac{\Lambda_i Y_i}{T} + \frac{1}{J} \left(\frac{\partial V}{\partial T} \right)_p + \frac{R}{J} \sum_{j} dx_j (1 + \ln X_j)_{\text{all species}} \right]}{\frac{C_p}{T} + \sum \frac{J\Lambda_i^2}{PT^2} (V_i - V_{\ell}) + \frac{L}{1 - L} \frac{C_{\ell}}{T}}$$

which tells how to step temperature with pressure during the condensation process.

The nozzle performance can then be evaluated at each station. A balance of pressure forces and mass times acceleration yields jet velocity increase between stations

$$\frac{udu}{g_c} = (1.033 \times 10^{6}) \frac{V_{ave}}{M_m} dp \text{ without condensation}$$

or

$$\frac{udu}{g_c} = (I - L)(1.033 \times 10^{-6}) \frac{V_{ave}}{M_m} dp \text{ with condensation}$$

Assuming only the vapor may do work by expanding, the average volume between stations is the volume at the previous station plus an incremental volume dV/2.

$$V_{ave} = V + \left(\frac{\partial V}{\partial P}\right)_T \frac{dP}{2} + \frac{\partial V}{\partial T} \frac{dT}{P^2}$$

The vacuum impulse is then computed by

$$I_{vac} = \frac{U}{g_c} + \frac{PA}{\omega}$$

The density is the mass per unit volume and, along with the continuity equation, this defines the cross-sectional area. From continuity, an expression for cross-sectional area is derived,

$$\rho U A = \dot{\omega}$$

This area is then tested against the area calculated in the previous step in pressure. A minimum area defines the throat, from which the area ratio is subsequently calculated. Not all the numbers generated have meaning until flow is choked by attainment of sonic velocity. As a check of specific impulse, a force balance is made for a unit flow rate of propellant.

$$Ze = 1033 \left[(PA)_{initial} + \int_{A_{initial}}^{A_{throat}} p\,dA + \int_{A_{throat}}^{A_{exit}} p\,dA \right]$$

This force Ze is the thrust which would be attainable by a unit weight flow rate of propellant.

The final step is to increment p (data) and T, Y, U, and L (calculated) and repeat the cycle. One of the options of the programs is to ignore condensation with consequent simplification of the calculations.

Problems were encountered, most of which are now solved. The validity of the treatment is indicated by the fact that entropy is constant, mass is constant, and the change in enthalpy equals the gain in kinetic energy. Further, momentum and pressure area integration yield the same specific impulse.

As of the present time, entropy of mixing, and Dalton's law of partial pressures are formulated in ideal gas ways. Consideration is being given to the conversion of these equations to their real gas counterparts, but since pressure is low, it is felt, at present, that the ideal gas formulation is less susceptible to error than is, for instance, the one-dimensional approximation. It should be noted that none of these ideal gas approximations is involved in cases in which only one component is present. Lastly, since the variance between ideal and real-gas performance is so small, the ideal-gas program may be used with some confidence in single component studies under the conditions of the present study.

SYMBOLS

A, a, B, b, c	Constants of Beattie Bridgman equation
Ai, ai, Bi, bi, ci	Individual constants of Beattie Bridgeman equation
$\alpha, \beta, \phi, \varphi$	Constants of specific heat equation
$\alpha_i, \beta_i, \phi_j, \varphi_j$	Individual constants of specific heat equation
Qi, Di, Si, Ni	Individual constants of vapor pressure equation
A	Area (cm^2)
C_ℓ	Specific heat of liquid (cal/gmole °K)
C_p	Specific heat of gas (or vapor) (cal/gmole °K)
g_c	Gravitational constant (980.7 cm/sec^2)
I_{vac}	Specific impulse exiting to vacuum (sec)
J	Conversion factor $\left[0.041343 \frac{1 \text{ atm}}{\text{g mole°K}} \Big/ \frac{\text{cal}}{\text{g - mole°K}}\right]$
L	Number of moles of liquid per mole of original material
M_m	Molecular weight of vapor (g/g-mole)
M_j	Molecular weight of species j (g/g-mole)
n	Number of increments in C_p calculation
P	Static pressure (atm)
P_j	Vapor pressure of component j (atm)

R	Gas constant, 082.06 $\frac{m\ell - atm}{g - mole°K}$
S	Entropy (cal/°K)
T	Temperature (°K)
u	Velocity (cm/sec)
V	Volume of the liquid (l/g-mole)
V_ℓ	Volume of the liquid (l/g-mole)
V ave	Ave volume of gas (or vapor) in interval between pressures P and P + dP (l/g-mole)
$\dot{\omega}$	Weight flowrate of propellant (g/sec)
Y_j	Mole fraction species j
Z	Thrust per unit weight flow rate of propellant (sec)
ρ	Density of the gas (or vapor) (g/cm^3)
ρ_L	Density of the liquid (g/cm^3)
Λ_i	Heat of vaporization of species i (cal/gmole)
Note:	Quantities to be averaged
	$\sum_j Y_j X_j$
	except for A, where $\sqrt{A} = \sum_i yi \sqrt{A_j}$

Subscripts:

i	Saturated species only
j	All species
k	All species
ℓ	Liquid

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APPENDIX J. ELECTROLYSIS CELLS

The following general comments are offered as an appraisal of the status of the development of electrolysis cells which produce separated gases in a zero-gravity environment.

DEVELOPMENT PROGRAMS

Several organizations (General Electric Missile and Space Company, Allis-Chalmers, General American Transportation Corporation, and Battelle Memorial Institute) are developing cells for incorporation into life support systems. While these units are often sized for the oxygen requirements of a four-man crew (8 pounds of oxygen per day), they are modular in design, and adaptation to other requirements should not be difficult. The units of most interest here are the cells designed for integrated life support systems in spacecraft, such as the developments supported by NASA/ Langley (Reference 1). Units being developed for use in aircraft generally employ a blower to circulate the humid breathing mixture over the electrolysis cell, a mode of operation that appears unpromising for the present application. A cell is also being developed for employment in submarine life support systems. This cell employs a circulating electrolyte and would therefore not be suitable for zero-gravity use.

AVAILABILITY OF DEVELOPED HARDWARE

No suitable off-the-shelf zero-gravity electrolysis unit of proved performance is available at the present time. A promising development program is currently being carried out by Allis-Chalmers for the ILSS study at NASA/Langley; the delivery of a unit to Langley for evaluation is anticipated in August 1967. Other development efforts are believed to be even further from realization (with the possible exception of the aircraft-type units mentioned above, which are not well adapted to reaction control system use).

An electrolysis cell of the ion exchange membrane type was developed for the original ILSS study program at NASA/Langley by General Electric Company (Reference 2). During subsequent testing, it was found that acidic liquid from the electrolysis cell entered the gas side of the system during the operation of the unit. The causes of this difficulty have not been positively identified at present, but some possibility exists that the liquid is forced through the membrane by electroösmosis during operation of the cell. It is therefore likely that further development effort is necessary on what might otherwise have been regarded as a developed unit.

ADAPTABILITY OF CELLS TO PRESENT REQUIREMENTS

Three areas of particular interest here are:

1) The simplicity and reliability of the mechanical design.

2) The longevity of the system in the space environment.

3) Power and efficiency.

Mechanical Complexity

In most cases, some mechanical components with moving parts are required for proper operation of the electrolysis cells which produce separated gases. This complexity may have an impact upon the reliability of the systems with stringent requirements.

For most types of cells under development, proper operation requires a complex pressure regulation system. For example, the operation of both the alkalimatrix cells (such as the TRW cell and the Allis-Chalmers cell) requires regulation of the pressure differences across the matrix to a few psia. The General Electric ion exchange membrane cell requires differential pressure regulation of the oxygen manifold, the hydrogen manifold, the nitrogen supply, and the water supply to differences of a few psia.

Failure of these pressure regulation systems would be likely to cause at least a temporary failure of the electrolysis cell. For example, the restriction of water flow to the electrolysis matrix of an alkali matrix cell because of low feed pressure would tend to favor mixing of the gases by diffusion through relatively dry areas of the matrix and the occurrence of hot spots in the matrix, tending to degrade the electrode and also cause further drying of the immediate area of the matrix. The return of the system to normal operating conditions would probably result in a usable, if somewhat degraded, electrolysis cell. On the other hand, an excessive water feed pressure might cause failure due to flooding of the cell with water. In this case, return of the system to normal operating conditions might not return the cell to servicable condition if too much electrolyte had been removed from the cell. Further, the presence of a strongly basic solution in the gas side of the system might lead to the failure of other components downstream of the electrolysis cell.

Cells which operate upon a circulating mixture of gas and water vapor would require a mechanical blower, which appears to introduce even greater mechanical problems than differential pressure regulation. Some degree of pressure regulation may also be required by these cells.

One type of cell, currently under development at Battelle Memorial Institute, offers the possibility of great mechanical simplicity in a separated gas electrolysis cell; this is the palladium-silver cathode cell (References 3 and 4). Because large pressure differentials (perhaps 2000 psia or more) can be sustained between the hydrogen gas system and the rest of the system, it is possible to eliminate at least one, and possibly all of the pressure regulators found in other systems. Such a capacity to sustain higher pressure differences between gas plenums might also improve system reliability by allowing electrolysis cell operation in the presence of excessive leakage rates from one of the gas plenums. In the other systems, the nonleaking plenum might have to be vented in order to maintain the proper pressure differential between various parts of the systems.

Longevity of Electrolysis Unit

One of the potential problem areas in which no firm information can be obtained until a body of test experience is accumulated is the anticipated useful life of the complete electrolysis unit, including all required pressure regulators, valves, and electrical power supplies. Insofar as the electrolysis cell itself is concerned, however, it appears that a useful life of many years can be achieved by proper selection of materials and operating conditions.

In general, a long cell life would be obtained by employing relatively thick, smooth electrodes of highly compatible materials instead of the high surface area type of electrode, and selecting lower operating temperatures and lower current densities than in current designs. Such a cell would certainly be heavier than a cell operating at high current densities. The power efficiency of the cell would probably be somewhat lower than cells of similar capacity which employ high surface area electrodes, although the reduction in current density will tend to improve the power efficiency of the cell.

Of particular interest in this respect are the results of long term tests with an "electrowinning" apparatus (Reference 15) in which over 14,000 hours of operation resulted in no degradation of the cell characteristics, and tests on a high pressure electrolysis cell (Reference 15) in which over 10,000 hours of operation (on an intermittent schedule) were accumulated with no changes in cell characteristics. In both of the above cases a circulating alkaline electrolyte was employed. Neither device, however, is capable of operation in a zero-gravity environment.

In another long-term test of electrode behavior, a palladium-silver alloy cathode was operated successfully for over 9,000 hours (Reference 3), even though this type of cathode is reported to be extremely sensitive to electrode poisoning.

From the long-term test data obtained to date, the tentative conclusion may be drawn that the limitations upon cell operating life will be due essentially to limitations on the life of mechanical parts such as pressure regulators, the electrical power supply, or, in the case of cells employing ion

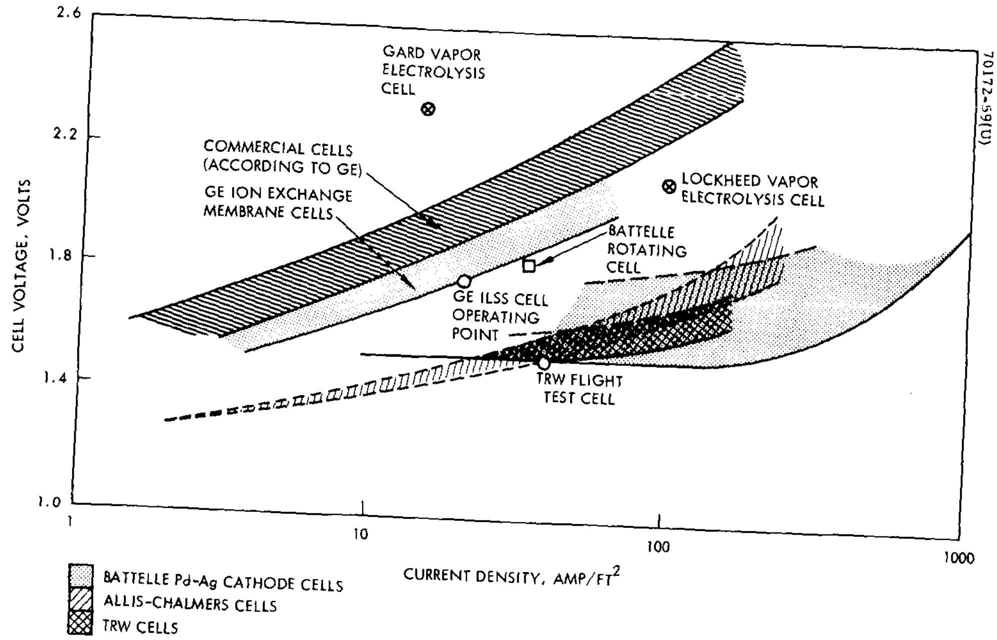

Figure J-1. Cell Voltage as Function of Current Density for Various Water Electrolysis Cells exchange membranes, to a slow deterioration of the membrane conductivity with use, and to the capability of the heat transfer mechanisms to maintain the cell temperature within satisfactory boundaries.

Power and Efficiency

Inasmuch as the design objectives of the cells described above differ somewhat from cell to cell, the units produced by the various manufacturers are not strictly comparable. It is of interest, however, to examine in a general way the relative efficiencies and weights of the various cells under development. One parameter which reflects cell efficiency is the cell voltage. At ordinary temperatures, the theoretical decomposition voltage for water is about 1.23 volts. Voltages over 1.48 volts result in the liberation of heat during the electrolysis reaction. (Reference 16)

On the assumption that the current efficiencies of all of the cells considered here are essentially 100 percent (that is, no current is consumed in side reactions or current loops which bypass the cell), the electrical power consumed by a cell will be proportional to the cell voltage, and the heat produced by the cell will be approximately proportional to the cell voltage minus 1.48 volts.

The current density of the cell is loosely related inversely to the size and weight of the cell. Since a given total current will be required to produce a given required weight of electrolysis products per unit time, a cell which operates at a higher current density will require a smaller electrode area, which will in turn allow a smaller overall cell size and weight. An inversely proportional relationship between these variables cannot be assumed, however, because cell size and weight also depend upon such factors as the area needed for sealing surfaces and manifolding, the operating pressure, the requirements for heat dissipation, and the ancillary equipment required (such as pressure regulators and valves).

The relationship between cell voltage and current density is summarized for a number of water electrolysis cells in Figure J-1. It is apparent that the generally desirable characteristics of high current density and low cell voltage are achieved best by alkaline matrix cells, such as those developed by the Allis-Chalmers Company and Thompson Ramo Woolridge Electromechanical Division, in the present state of the art. The palladium-silver cathode cell, which is at present incorporated only in experimental devices, appears on the basis of this evaluation to be the most promising cell for future development.

Present state-of-the-art cells employing ion exchange membranes, such as the General Electric cell, are limited in current density and also have somewhat higher cell voltages than the alkaline matrix cells. The cells designed to electrolyze the water removed from a circulating cabin air stream by a hygroscopic electrolyte bed give both the lowest current densities and the highest cell voltages.

However, neither the high efficiency nor low weight, which has been achieved only at great cost in existing designs, is important for the electrolysis plenum or dual mode cell. On the contrary, it is desirable to trade weight and efficiency, neither of which is critical in the intended application, for reliability and durability, which are of paramount importance on a long mission in space.

DETAILED DISCUSSION

Types of Electrolysis Cells and Systems

Two types of gas storage systems are considered:

1) The separated gas system, in which the gaseous products from the anode and the cathode are stored in separate pressure vessels (when mixing of the two gases is not desired).

2) The mixed gas system in which the anode and cathode products are mixed and are stored in a single pressure vessel (which may also provide for the storage of the electrolyte and the housing for the electrolysis cell).

System Requirements and Problem Areas

Separated Gas Cells

1) Regulation of the feed rate

In many of the water electrolysis cells being developed at the present time, a hygroscopic electrolyte is placed in a thin matrix between the electrodes and the water is fed to the cell as a vapor. The result of an excessive feed rate may be the flooding of the cell, leading to a possible loss of electrolyte and partial or complete cell failure. An inadequate feed rate may result in the development of "hot spots" within the cell, which in turn may give rise to excessive corrosion rates or large increases in the permeability of the matrix to the gases, or both.

2) Differential Pressure Regulation

In most cells which produce separately stored gases, the matrix or other material separating the anode and cathode compartments cannot withstand a large pressure differential. The relative pressure on the liquid feed system also may be important in the regulation of the feed rate. As a result, most cells under development require regulation of pressure differential across the constituent parts of the cell.

3) Separation of gas and liquid phases in zero gravity

The separation of the liquid phase from the gases to be fed to the thrusters is required. In most designs for zero-gravity conditions, the gas-liquid interface is maintained at the electrode surface by the cell design as it is in fuel cells. (An exception is the rotating cell built by Battelle which uses centrifugal forces to separate the gas from the liquid.) Wicks may also be incorporated in the gas lines to remove any liquid that may appear on the gas side from condensed vapor or other causes.

4) Other problem areas

Mechanical problems, such as seal leakage, and compatibility problems with the materials of construction and the electrolyte, may also occur.

<u>Mixed Gas Cells</u>

1) Zero-gravity operation

A separation of the gas phase from the liquid phase also must be effected in the mixed gas cell. Unlike the separated gas cell, there is no need to provide a barrier between the anode and cathode compartments which is relatively impermeable to gases. In this case, a weight reduction will probably be obtained by incorporating the electrolysis cell, the gas storage, and the electrolyte storage within the same tank. The problem of separating the gas phase from the liquid phase then becomes similar to the problem of venting the pressurizing gases from a propellant tank under zero-gravity conditions.

Bell Aerosystems Company has made a study of the latter problem (Reference 5) and has suggested several solutions. Since both hydrazine and water have high surface tensions (67 and 72 dynes cm^{-1} at 25° C, respectively), there are many available materials of construction which are not wet by these liquids. The available data indicate that such venting can be accomplished without great difficulty.

2) Other problem areas

Mechanical and compatibility problems similar in nature to those of the separated gas cell are anticipated. The problems may be considerably reduced, however, by incorporating all electrolysis cell system functions within a single pressure vessel and by eliminating pressure regulators.

SURVEY OF SEPARATED GAS WATER ELECTROLYSIS CELL TECHNOLOGY

A survey of the current technology of water electrolysis to produce separated hydrogen and oxygen gases under zero-gravity conditions has been carried out. Visits have been made to the facilities of several of the manufacturers of zero-gravity water electrolysis cells, and to NASA/Langley, a user of electrolysis cells for the study of integrated life support systems (ILSS).

The development of several types of electrolysis cells is currently being funded by various government agencies. It is very likely that in-house projects are also being pursued by several organizations.

A list of organizations known to have investigated zero-gravity electrolysis is given in Table J-1. Many of these organizations are fuel cell manufacturers who base their electrolysis cell designs upon the features of fuel cells developed for zero-gravity operation. To a first approximation, these electrolysis cells are simply fuel cells "run backwards".

Nearly all of the electrolysis cells reviewed here are being developed for life support systems (i. e., to supply breathing oxygen). Some electrochemical cells are included in the survey although they are not electrolysis cells (such as the Allis-Chalmers apparatus for "electrowinning" oxygen from air) or are not intended for zero-gravity operation (e. g., the Allis-Chalmers electrolysis cell for Oak Ridge). These cells are similar in certain respects to the zero-gravity cells, however, and the data accumulated during the testing of these devices can be of benefit in assessing the prospects for obtaining long-lived and reliable electrolysis cells for space vehicles.

A discussion of the individual cells follows.

Wright-Patterson Air Force Base

Zero-Gravity Experiment Cell

An experimental electrolysis cell was fabricated in-house by a group at Wright-Patterson Air Force Base and flown as a "piggyback" experiment on a ballistic missile shot in November 1965 (Reference 6). The zero-gravity portion of the flight lasted between 22 and 23 minutes. No technical reports of this experiment have been published as yet.

Description of Electrolysis Unit. The electrolysis unit employed in this test was a stack of six cells housed in a polyethylmethacrylate case. An alkaline electrolyte in an asbestos matrix was held between nickel electrodes. The cells were fed by wicks from a water reservoir. The instrumentation consisted of pressure transducers on the hydrogen and oxygen sides of the cell and electrical measurements of cell performance.

TABLE J-1. ZERO-GRAVITY ELECTROLYSIS CELL MANUFACTURERS AND USERS

Organization	Project Manager or Other Authority	Major Interest
Allis-Chalmers Corporation Milwaukee, Wisconsin	P. Anthony	Electrolysis cell and fuel cell manufacturer
Battelle Memorial Institute Columbus, Ohio	J.W. Clifford	Electrolysis cell; other life support system component research and development
General American Research Division, General American Transportation Corporation, Niles, Illinois	R.A. Bambanek	Life support systems
General Electric Missile and Space Company Valley Forge, Pa.	Gordon Fogal	Electrolysis cell, fuel cell manufacture
Hamilton-Standard Division United Aircraft Company Windsor Locks, Conn.	E. Geddes	Life support systems
Lockheed Aircraft Corporation Palo Alto, California	W.J. Conner, et al	Electrolysis cell studies
NASA Ames Research Center Moffet Field, California		Electrolysis cell studies, life support systems
NASA/Langley Research Center Hampton, Virginia	R.W. Johnson	Life support systems
Pratt & Whitney Division United Aircraft Company Hartford, Connecticut	W. Lueckel	Fuel cell manufacture
U.S. Air Force, Aerospace Medical Research Labs., Wright-Patterson AFB, Ohio	R.E. Bennett	Life support systems
U.S. Air Force Flight Dynamics Labs., Wright-Patterson AFB, Ohio	Lt. M.A. Maxwell	Orbital test of electrolysis cell
TRW Electromechanical Division Euclid, Ohio	C. Fetheroff	Fuel cell, electrolysis cell manufacture

Test Results. The analysis of the flight test results is not yet complete and no report has been published. A preliminary look at the result indicated that the pressure of both the oxygen and hydrogen sides rose smoothly throughout the entire flight, including the boost phase. No change in rate was observed during the boost phase. Only a fraction of the water required to produce the observed pressure rise was originally present in the electrolyte matrices; thus the proper operation of the wick feed as well as the electrolysis cells were demonstrated.

It would appear that this experiment provides substantial evidence that properly designed electrolysis cells will operate under zero-gravity conditions.

General American Research Division (GARD), General American Transportation Corporation

Cell for Life Support System Studies

Small scale cells suited for life support system applications have been tested over the past 4 years at GARD (Reference 7). In general, these cells employ a hygroscopic jelly composed of H_3PO_4, P_2O_5, and various additives to remove moisture from a circulating airstream; the moisture is electrolyzed, releasing oxygen to the circulating airstream.

In the electrolysis cell effort, GARD has done work on cells employing microporous and ion exchange membranes. No published reports on this work are available. GARD has submitted proposals on electrolysis cell development work to the Air Force (Wright-Patterson AFB, Life Support Division) and to NASA (Ames Research Center and Langley Research Center).

In the life support system area, GARD has investigated Bosch reactor systems for the Navy and Sabatier reactors for the Air Force.

Description of Electrolysis Cell

The major difference between the GARD cell and other similar cells is the incorporation of a microporous polyvinylchloride film between the electrodes which acts as a barrier to the diffusion of the product gases across the cell. The microporous film is sealed to the cell frame, which is also fabricated of polyvinylchloride. An electrolyte-holding matrix of glass fiber reinforced polyester is placed on each side of the microporous screen, and platinum screen electrodes outside of the matrix complete the structure. In their present state of development, the efficiency of these cells is quite low. At a current density of about 20 amperes ft^{-2}, cell voltages range from 4.0 to 4.5.

Conclusions

1) The requirement for vapor feed from a circulating air stream makes these cells unattractive for the purpose considered in the present study.

2) A cell of this type would probably be much more susceptible to flooding and the subsequent loss of the electrolyte than cells designed for a liquid feed, such as the Allis-Chalmers, TRW, or General Electric Cells.

3) The efficiency of these cells is much lower than that of cells with comparable capacity which employ alkaline electrolytes.

4) This type of cell may not be as susceptible as other types of cells to failure (leakage of gas through the matrix) in the event of inadvertent drying.

5) These cells are specifically designed for separated-gas operation. If they were to be developed for the pressurization of tanks in unmanned vehicles, some system of differential pressure regulation would undoubtedly be required to prevent mixing of the generated gases.

6) Cells of this type are not available as off-the-shelf-units at the present time.

Battelle Memorial Institute

Several types of water electrolysis cells for life support systems have been investigated at Battelle. These include:

- Rotating cells, which separate the evolved gases from the electrolyte by centrifugal force.
- Vapor electrolysis cells employing a hygroscopic H_3PO_4 electrolyte to remove water vapor from a circulating atmosphere.
- A palladium-silver cathode cell, which utilizes the permeability of the Pd-Ag alloy to hydrogen in order to separate the hydrogen from the oxygen and the electrolyte.

Rotating Cells

Work on rotating electrolysis cells has been pursued at Battelle since about 1960 under contracts from the Air Force Systems Command at Wright-Patterson AFB.

The development of a two-man unit was completed in 1962 for the Aerospace Medical Division of the Air Force Systems Command (Reference 8), Contract AF 33(616)-7351.

In order to achieve long cell life, conventional commercial materials were selected; the cell employed nickel anodes, steel cathodes, and an asbestos separator with an alkaline electrolyte. A complete unit weighing approximately 284 pounds was delivered to the Air Force for evaluation.

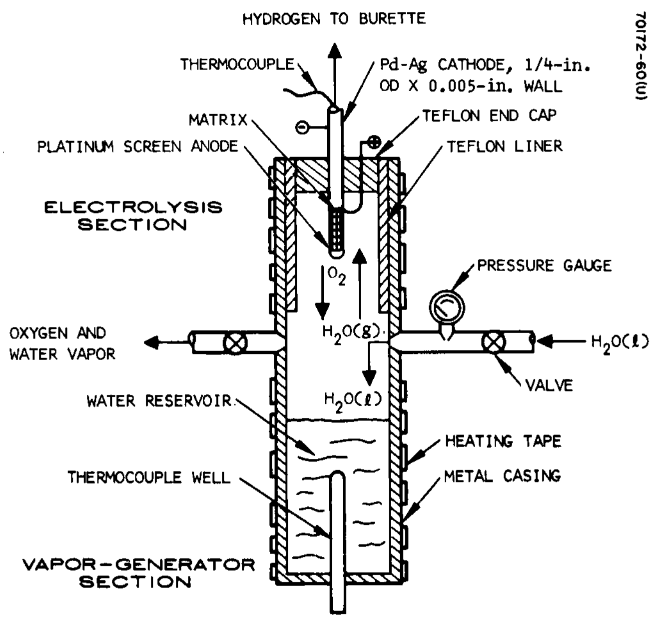

Figure J-2. Battelle Palladium-Silver Cathode Cell

At the present time rotating cell work is centered upon the development of a working system which electrolyzes 6.75 pounds of water per day. This work is being carried out under a contract to the Flight Dynamics Laboratory at Wright-Patterson AFB. The new cell design employs bipolar plates instead of the separate cathode and anode design of the previous unit. The weight of the complete unit is about 140 pounds. At the operating point the current density is approximately 37 amperes ft^{-2} at a cell voltage of 1.87. As in the previous work, a very conservative approach to cell design has been taken; catalysts have not been employed on the electrode surfaces, for example, because experience indicates that such catalysts frequently migrate from the electrode during cell operation, causing a degradation of the cell performance. The cell design is said to be capable of continuous operation at current densities of three times the design point current density.

Vapor Electrolysis Cells

The group at Battelle is developing a vapor electrolysis cell employing H_3PO_4 as a hygroscopic agent and electrolyte under a contract from NASA/Ames (NAS 2-2156) (Reference 3). Some previous related work was done under Air Force contract AF 33(657)-9355 (Reference 9). These cells are designed to operate in a stream of circulating cabin atmosphere at 75°F and 40 percent relative humidity. A platinized platinum cathode is employed to reduce the hydrogen overvoltage.

Several continuous runs of over 1000 hours and one continuous run of over 1600 hours have been made during testing of these cells. It is anticipated that at least one complete unit will be tested to over 3000 hours to demonstrate the long life capability of this design.

It has been found that the platinum black coating has been degraded on some but not all electrodes during these extended periods of operation. The causes for the breakdown of the platinized coating have not yet been determined.

The complete unit is composed of a stack of electrolysis cells which is approximately 6 by 3 by 3 inches in size and a small blower approximately 3 inches long and 3 inches in diameter.

Cells Employing a Palladium-Silver Alloy Cathode

The application of the hydrogen transmitting capability of certain palladium-silver alloys to the problem of separating hydrogen and oxygen gases evolved during the electrolysis of water has been studied at Battelle under several contracts (AF 33(616)-8431 (Reference 10), AF 33(657)-10988 (Reference 4), and AF 33(615)-2954).

In the work at Battelle, a thin sheet or tube of the properly annealed Pd-Ag alloy forms the cathode of the electrolysis cell. Anodes are formed from various materials; platinum screens are often used. An example of an experimental cell operating with a vapor feed is shown in Figure J-2.

(taken from Reference 8). Various bases, including KOH, $Ca(OH)_2$, and NaOH, have been employed as electrolytes. It has been found that all of th hydrogen generated at the electrolyte-cathode interface will be transmitted through the Pd-Ag alloy and released on the "dry" side of the electrode und the proper experimental conditions, that is, when the cell is operating at a current density below the critical current density value. The critical curr density value can be surprisingly high. For example, data from one set of experiments at 125°C in 66 percent KOH solution gave current densities of 230 amperes ft^{-2} through a 5-mil sheet of Pd-Ag alloy, and calculations indicate that current densities of 1150 amperes ft^{-2} would be possible with a 1-mil Pd-Ag alloy sheet as cathode (Reference 8).

It should be noted, however, that serious problems attend the use of the Pd-Ag cathode. The hydrogen transmitting capability of this material can be seriously impaired by minute traces of poisoning material. It has been found, for example, that the best of the available asbestos matrix materials cannot be used because the residual impurities rapidly poison the cathode. Electrolyte chemicals meeting the chemically pure (C. P.) standards of the American Chemical Society must be further purified before they can be employed. In spite of these difficulties, however, an experimental cell of this type has been operating continuously at Battelle for well over 1 year at 37 amperes ft^{-2}.

One of the most interesting features of this type of cell is the capability for generating oxygen and hydrogen at two widely different pressures. It may be possible, for example, to generate hydrogen at a high pressure, such as 2000 psia, while the electrolyte side (and oxygen side) of the cell is at a low pressure (Reference 3).

Conclusions

1) The employment of a mechanically rotated cell appears at first glance to be an unnecessarily complicated solution to the problem of zero-gravity electrolysis cell operation. For life support systems, however, in which high reliability and fail-safe features are essential and some maintenance may be available, these cells offer certain advantages. Simplicity and reliability of electrochemical function are obtained at the cost of mechanical complexity.

   The Battelle unit appears to be in a late stage of prototype development and may be ready for the development of flight-type systems in the near future.

   While a rotating cell might be suitable for manned space vehicles, it appears to be unsuitable for the mission considered here. The problems attendant to the design of a highly reliable system with rotating gas and liquid seals and electrical connections which can operate without maintenance for the duration of the mission appear to be severe. Some power would also be required to rotate the electrolysis unit.

In the case of a spinning vehicle, designs similar to the rotating cell described above might be considered more favorably.

2) The vapor electrolysis cell being developed at Battelle appears to be essentially a flight prototype configuration. This cell is similar to those being developed by GARD and Lockheed which remove water vapor from a cabin atmosphere by circulating the atmosphere over a hygroscopic material which also serves as the electrolyte of the cell. Cells of this type are judged to be less suitable for the purposes considered here than the other types of zero-gravity cells being developed. The reasoning behind this judgement is given in the comments under the GARD cell, which also apply in general to all water vapor electrolysis cells.

3) The development of a cell utilizing a Pd-Ag alloy cathode would offer many advantages for a long duration unmanned mission.

   a) The stringent differential pressure regulation required over the entire operating pressure range of the system for cells which employ a thin asbestos or ion exchange membrane to achieve separation of the generated gases is not required for the Pd-Ag cathode cell. As mentioned, there appears to be no reason why pressure differentials of several thousand psia cannot be routinely obtained across a Pd-Ag cathode with adequate mechanical strength. The increase in all voltage necessary to generate pressures of several thousand psia would be only a few millivolts.

   b) The probability of failure due to cell flooding and electrolyte loss would be much less for a vapor feed cell employing a Pd-Ag cathode, since the oxygen and the liquid source can be maintained at the same pressure without reference to the hydrogen pressure (or tank volumes), and differential pressure regulation is eliminated. A similar reduction in the complexity of a liquid feed cell would be obtained. Under these conditions, capillary methods for zero-gravity separation of the oxygen and the stored water or electrolyte would be feasible. Allowing the cell to stand idle for long periods of time would also pose no problem due to the mixing of hydrogen and oxygen by diffusion across the cell barrier.

   c) High current densities appear to be feasible with this type of cell, allowing compact and lightweight units to be fabricated.

4) The longevity of the Pd-Ag cathode cell may be a matter of some concern, since the cathode is extremely sensitive to

poisoning. There appears to be some basis for confidence that this problem can be satisfactorily resolved, however, since:

a) Preliminary work at Battelle has identified at least one of the poisoning agents (Fe). A method for producing NaOH or KOH which does not give cathode poisoning has been developed.

b) A test cell has been operated continuously for over a year at a current density of 37 amperes ft^{-2}. Many of the better developed electrolysis cells investigated in this study have not demonstrated this degree of longevity.

5) An off-the-shelf flight qualified electrolysis unit employing a Pd-Ag cathode is probably several years away.

General Electric Company, Missile and Space Division (Valley Forge Space Technical Center)

ILSS Electrolysis Unit

The development and fabrication of a water electrolysis unit was carried out by General Electric Company under a subcontract from General Dynamics/Convair (NAS 1-2934) for inclusion in the Integrated Life Support System (ILSS) study at NASA/Langley. Development of this equipment was initiated about 3 years ago.

For the purposes of the study, hardware requirements were defined by assuming a four-man orbital mission of 1 year duration, with resupply at 90 day intervals. The quality of the feed water, electrical limitations, cooling limitations, maintenance requirements, and quality of generated gases were specified. A design which was suitable for zero-gravity operation was also required, but actual testing under gravity conditions was not planned.

It is understood that a unit similar to the Langley unit has been installed in the Hamilton Standard Chamber at the Aerospace Medical Laboratory at Wright-Patterson AFB. This unit is reported to be a one-man unit (2 pounds of oxygen per day) which employs only 12 cells in a stack.

Description of Electrolysis System. The electrolysis unit developed under the ILSS contract (References 2, 11, and 12) is composed of a control module and three electrolysis modules. Each electrolysis module contains a stack of electrolysis cells and the associated hardware. The complete system is 20 by 20 by 36 inches in size and weighs approximately 180 pounds. Approximately 950 watts of electrical power is required (24 to 32 volts) for the operation of the system. The rate of electrolysis is 9 pounds of water per day.

A diagram of a modular unit and its associated plumbing is shown in Figures J-3 and J-4 (taken from References 11 and 2 respectively). The components and their functions are given in Table J-2.

Each module contains a stack of 16 individual electrolysis cells connected electrically in series, together with the necessary end plates, bolts, plumbing, and electrical connections. A wick separator device (Figure J-5) is mounted on each end of the stack to provide for the removal of any liquid from the H_2 and O_2 gas streams under zero-gravity conditions. The liquid is returned to the water reservoir. The stack assembly is contained in a housing which is pressurized by N_2 to approximately 1.5 psia above the pressure of the product gases.

In addition to the wick separator devices for removal of liquid from the gas streams during zero-gravity operation, each product gas stream was also provided with a conventional "1-g" trap for the removal of liquids during ILSS studies at NASA/Langley.

Rather stringent control of pressure differentials within the electrolysis cell is required during electrolysis. The following relationships between various pressures within the system are provided by mechanical differential pressure regulators:

$P(N_2)$ = regulated pressure of about 9.0 psia

$P(H_2)$ = $P(N_2)$ - 2 psia

$P(O_2)$ = $P(H_2)$

$P(H_2O)$ = $P(H_2)$ - 1.5 psia

In addition to the differential pressure regulation indicated here, a number of the solenoids indicated in Table J-2 are actuated by pressure switches; relief actuation pressures are:

Sol -1	7.0 psig
Sol -2	8.0 psig
Sol -3	8.0 psig
Sol -4	6.5 psig
Sol -5	6.5 psig
Sol -6	6.5 psig

The basic structure of the individual electrolysis cells is illustrated in Figure J-6. Two ion exchange membranes (sulfonated polystyrene on a matrix of flourocarbon material) contain the electrolyte (25 percent by weight H_2SO_4). The electrode material, platinum black, is coated on the membrane. Electrical contact is provided by a titanium-palladium alloy current carrier. The current carrier and membrane, together with required feedthroughs, are bonded with epoxy to a glass cloth reinforced epoxy frame. The electroltye-filled space between electrodes is maintained by a ribbed spacer of Hypalon

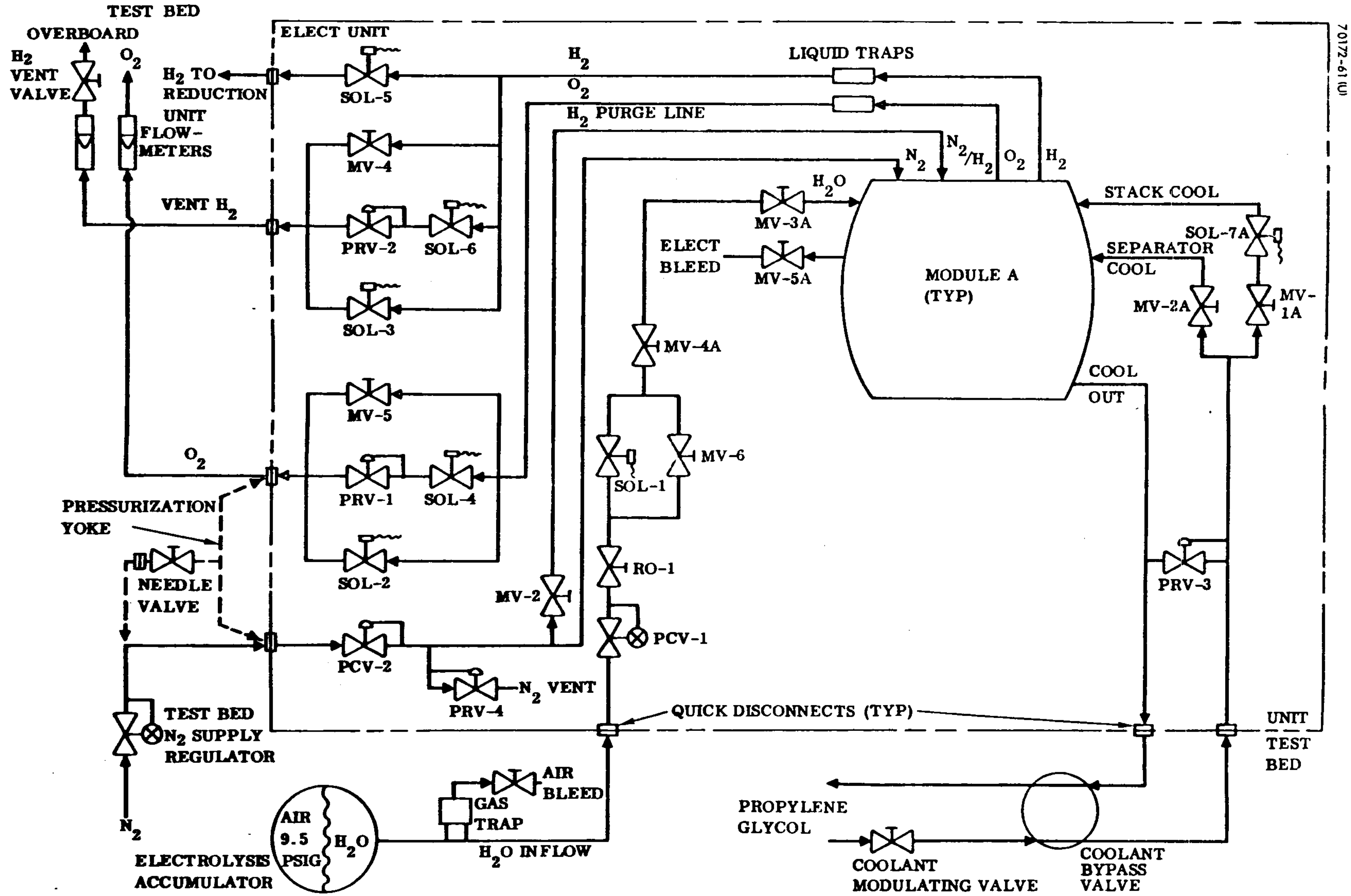

Figure J-3. General Electric Water Electrolysis Unit Schematic

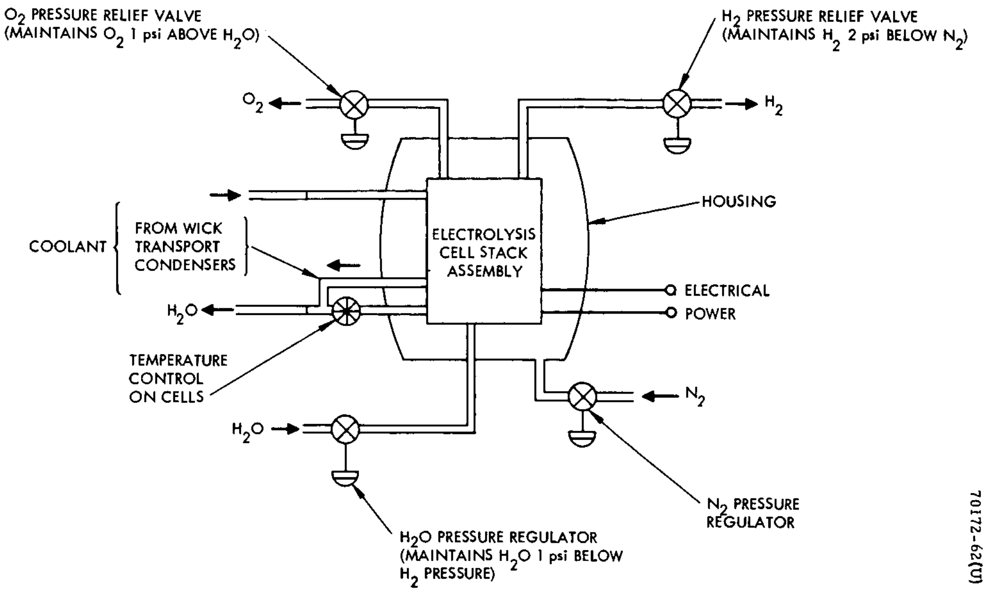

Figure J-4. General Electric Electrolysis Cell Stack Assembly Schematic

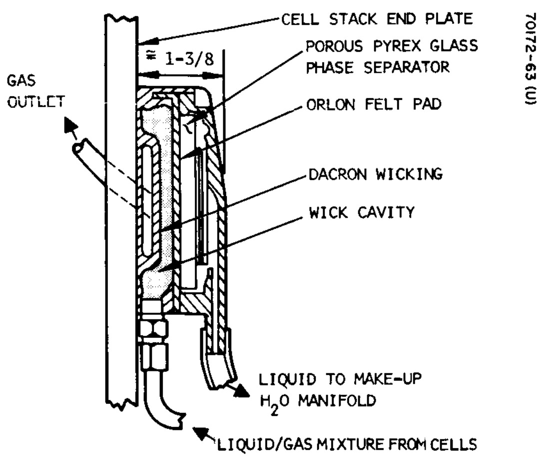

Figure J-5. General Electric Cell Wick Transport Circuit Cross Section

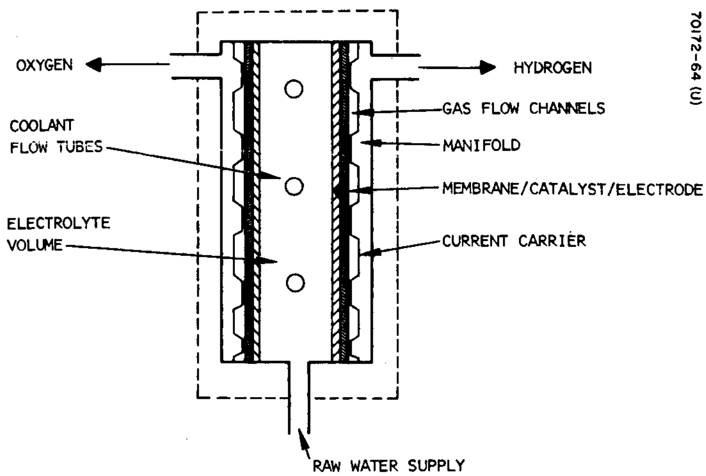

Figure J-6. General Electric Electrolysis Cell Functional Schematic

TABLE J-2. ELECTROLYSIS UNIT VALVE FUNCTIONS*

Valve Symbol	Valve Type	Function
PCV-1	Pressure regulator	Controls feed water pressure to the modules
PCV-2	Pressure regulator	Backup control for N_2 pressure to the modules
PRV-1	Back pressure regulator	Controls module O_2 pressure
PRV-2	Back pressure regulator	Controls module H_2 pressure
PRV-3	Back pressure regulator	Limits unit coolant pressure differential
PRV-4	Back pressure regulator	Relieves N_2 overpressure
MV-2	Manual metering valve	Connects N_2 circuit to H_2 circuit for purge and pressurization
MV-4	Manual metering valve	Bypasses H_2 regulator and vents H_2 circuit
MV-5	Manual metering valve	Bypasses O_2 regulator and vents O_2 circuit
MV-6	Manual metering valve	Bypasses water shutoff solenoid valve
RO-1	Manual metering valve	Meters and shuts off water inflow
MV-1A, B & C	Manual metering valve	Modulates stack coolant flow
MV-2A, B & C	Manual metering valve	Modulates wick transport coolant
MV-3A, B & C	Manual metering valve	Module water feed shutoff at module
MV-4A, B & C	Manual metering valve	Module water feed shutoff at unit
MV-5A, B & C	Manual metering valve	Electrolyte bleed shutoff
Sol-1	Solenoid valve (NC)**	Automatic water feed shutoff
Sol-2	Solenoid valve (NC)	O_2 high-pressure relief
Sol-3	Solenoid valve (NC)	H_2 high-pressure relief
Sol-4	Solenoid valve (NO)	O_2 low-pressure shutoff
Sol-5	Solenoid valve (NO)	H_2 low-pressure shutoff to reduction unit
Sol-6	Solenoid valve (NO)	H_2 low-pressure shutoff
Sol-A, B & C	Solenoid valve (NO)	Module on-off coolant flow control

*Reference 12.

**NC = Normally Closed.
NO = Normally Open

rubber. Cooling of the cell is accomplished by a flow of proylene glycol through a 0.125-inch diameter stainless steel tube in the electrolyte volume.

The complete stack of electrolysis cells is approximately 6.5 by 7.75 by 8.5 inches in size.

Performance of Electrolysis Unit. Cell voltages as a function of current density for the General Electric cells are given in Figure J-7 (Reference 2). The units described here are designed to operate at a current density of approximately 25 amperes ft^{-2}, which gives a voltage drop of 1.8 volts per cell in a new unit. A current density of 100 amperes ft^{-2} is considered to be a high value for this type of cell.

The voltage required to obtain a given current density has been observed to increase slowly as the cell ages. Three factors which appear to contribute to the increase in cell voltage are membrane aging, loss of electrode area, and increases in the resistance between the current carrier and the electrode.

The rate of membrane aging is said to be a function of operating temperature and the applied voltage. A relatively recent development, the "S-Membrane" (Reference 12), is said to be much less sensitive to applied voltage than the older types of membrane. More detailed information was not available concerning either the magnitude of this effect or the mechanism by which the ion exchange membrane degradation occurs.

Separation of platinum black electrode material from the ion exchange membrane is a well documented phenomenon (Reference 11) and has occurred in several tests. In at least one case, particles of electrode material were found in liquid carried in a product gas line, and inspection revealed that free particles of platinum black were distributed over interior surfaces of the cell. Transport of platinum through the membranes has also been reported.

Areas of contact between the current carriers and the electrode surface exhibited nearly complete removal of the platinum black in some cases, and in other cases the contact between platinum layer and current carrier was observed to be poor (Reference 11).

With regard to demonstrated electrolysis cell longevity, General Electric has operated several cells for as long as 4000 to 5000 hours, and one cell has been operated satisfactorily for more than 60,000 hours (0.7 year) (Reference 12). In general, however, testing experience has been limited to runs of a few hundred hours. It is believed that testing in the related area of fuel cells employed in exchange membrane separators (such as the Gemini fuel cells) also has been limited to a few hundred hours in most cases.

Operation of the electrolysis cell under zero-gravity conditions has not been experimentally demonstrated. Some confidence in the capability of the cell to operate under zero-gravity conditions is gained from the reported ability of the electrolysis cells to function upside down and from tł successful operation of the Gemini fuel cells, which are similar in design, under zero-gravity conditions (Reference 12).

The mechanical design of the electrolysis cells has given rise to several difficulties in the operation of the cell (Reference 11):

1) Numerous leaks were noted in the connections between Tygon tubing and the metal tubes from the electrolysis cells or the manifolds.

2) Bonds between metal parts of the current carrier-electrode-membrane-frame assembly and the frame itself were not adequate in many cases to prevent separation of the parts, which resulted in leakage from the cell.

3) The rubber cell spacer and electrolyte container was compressible enough to reduce significantly the spacing between electrodes. No ill effects from this compression were noted, however, in the electrical performance of the cell.

4) Leakage around seals was often experienced, and corrosion resulting from the action of the strongly acetic electrolyte on noncompatible materials was often noted.

5) Pressure regulators failed to operate within acceptable limits in some cases.

6) In several cases the mixing of hydrogen and oxygen, and subsequent reaction of these gases upon the platinum surface, appeared to be the cause for a rather rapid degradation of individual cell characteristics.

7) Electrolyte carryover into the gas side (hydrogen side) of the cell has been repeatedly observed during testing of these cells, causing corrosion problems.

Pressure regulators and pressure switches, in particular, have been affected. In order to perform system tests, it was decided to place conventional "1-g" liquid traps in the gas lines from the cells in order to protect downstream components. Design changes in the wick separator also were made. The cause of this migration of electrolyte collects in the gas passages during periods of standing. It has been suggested that this behavior may be an electroosmotic pumping effect inherent in the mechanism of operation of the ion exchange membrane.

Conclusions.

1) The electrolysis unit fabricated for the ILSS contract is believed to represent a relatively high state of development for an electrolysis cell employing solid ion exchange membranes for zero-gravity containment of the electrolyte. An extensive background in fuel cell technology (including the development of qualified flight hardware on the Gemini program) has been available.

2) Zero-gravity operation of this electrolysis cell has not been demonstrated by actual test. However, the successful operation of the Gemini fuel cells, which have a similar design, and the demonstrated upside down operation of the electrolysis cells, offers considerable evidence that zero-gravity operation would be satisfactory, provided the effects referred to in items 3 and 4 below can be eliminated or controlled.

3) The problems brought to light in the operation of the ILSS unit appear to fall into two groups:

   a) Mechanical problems, such as leaking seals and joints and inadequate structural strength of the cell frame. Relatively small development efforts should quickly provide solutions to these problems.

   b) Technical limitations of the design concept, such as the loss of platinum electrode material from the surface of the ion exchange membrane, the transfer of platinum through the membrane, the necessity for rather precise pressure regulation, the increase in membrane resistance with age, and the transfer of water and electrolyte through membrane during electrolysis. It seems likely that the satisfactory resolution of these problems will require a more extensive effort, including, perhaps, the development of new materials and hardware items. In the case of the appearance of electrolyte solution in the hydrogen manifold, the possibility that the liquid is transported through the membrane itself by electroösmosis during cell operation raises some doubt concerning the validity of the basic concept, since under these conditions the wick for removing the transported electrolyte, and not the ion exchange membrane, is acting as the zero-gravity separator of the liquid electrolyte and the gas phase.

   With respect to the problems which were encountered in the NASA/Langley study, it should also be noted that the rather erratic appearance of some of the difficulties, such as the loss of platinum from the electrode coating on the membrane or the sudden degradation of a few cells in a stack, would be an important consideration when the system reliability is assessed for a long duration mission. (Other users have reported similar problems with the currently available platinized platinum electrodes; see, for example, the discussion of the work at Battelle.)

4) The maximum current density obtainable over long periods of time with this type of cell (about 100 amperes ft^{-2}) is considerably lower than the maximum current densities obtainable from KOH-asbestos matrix cells, some of which can probably operate for extended periods at well above 1000 amperes ft^{-2} without apparent degradation if adequate cooling is provided.

The cell developed under the ILSS contract also exhibits a larger voltage drop (1.8 volts) at its operating point (25 amperes ft^{-2}) than the typical KOH-asbestos cell, which would exhibit a voltage drop of approximately 1.6 volts at a current density of 300 amperes ft^{-2}.

5) In view of the many problems encountered during testing of the ILSS unit, this cell cannot be regarded as available "off-the-shelf" for missions of the type considered in the present study. Because of the relatively basic nature of some of the reported problem areas, it is estimated that an electrolysis cell of this type which would be qualified for the mission of interest here is probably several years away.

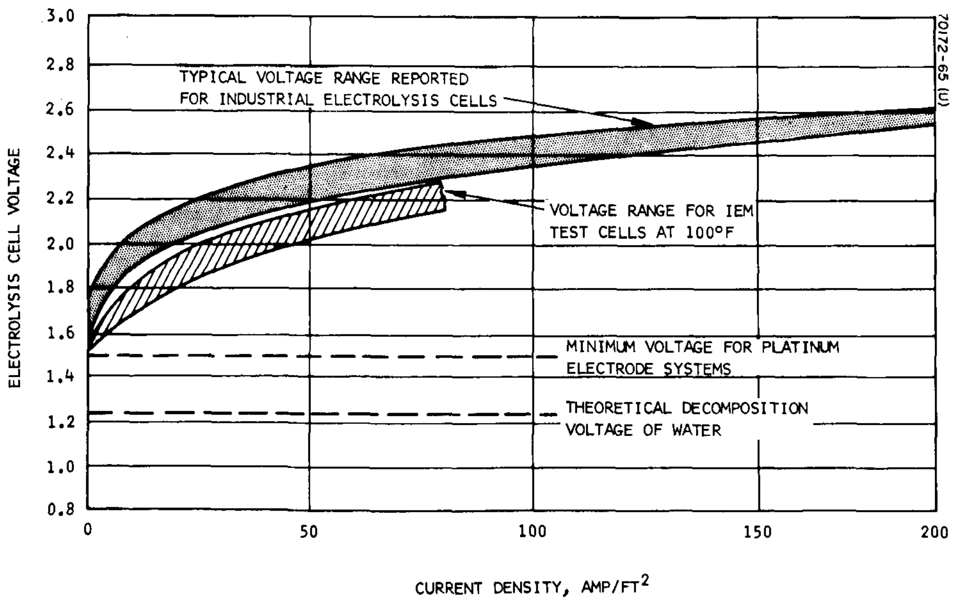

Figure J-7. General Electric Cell Voltage as Function of Current Density

TRW Electromechanical Division

TRW Cell for Orbital Test

Under NASA Contract NASw-998, a system composed of a zero-gravity water electrolysis cell and the associated instrumentation was developed for flight testing on an orbiting vehicle (References 13 and 14). Two flight units, each weighing 14.5 pounds, were fabricated and delivered to NASA in the summer of 1966.

Description of Electrolysis Cell. The design of the cells developed under this contract differs from the TRW cells built previously in that the water vapor is supplied to the electrolyte matrix of each cell by diffusion from a water transport matrix, which is in turn fed by liquid water from this reservoir. Pressure regulation of the gases in the cell is provided by laminar flow tubes of capillary size which dump generated gases into the space vacuum; water pressure is determined by a spring acting on a diaphragm in the water reservoir. This design therefore provides pressure regulation during orbital test without the introduction of dynamic components for pressure regulation into the system.

Description of Flight Test System. A block diagram taken from Reference 13 which shows the overall system, is shown in Figure 5-8.

The principal parts of the system are the electrolysis cell stack, its associated power supply (not shown), the water reservoir, the fill system, the analytical trains, and the capillary tubes employed for back pressure regulation.

It is anticipated that the electrical power to the stack will be supplied at constant current, and cell voltages will give data on the performance of the unit.

The construction of the electrolysis cell stack is indicated in Figure J-9 which is taken from Reference 13 with some modifications to bring the diagram into agreement with the text and figures.

The cells are stacked in a bipolar plate type of construction. Electrical current is supplied to the stack through connections on the plates labelled (2) and (5) in the figure. Voltage taps are placed on each oxygen electrode to provide performance data for each cell.

Design point specifications of the cell include:

Current density	100 amperes ft^{-2}
Area (per cell)	9 square inches
Current	6.25 amperes
Electrolyte	32 percent KOH (by weight)

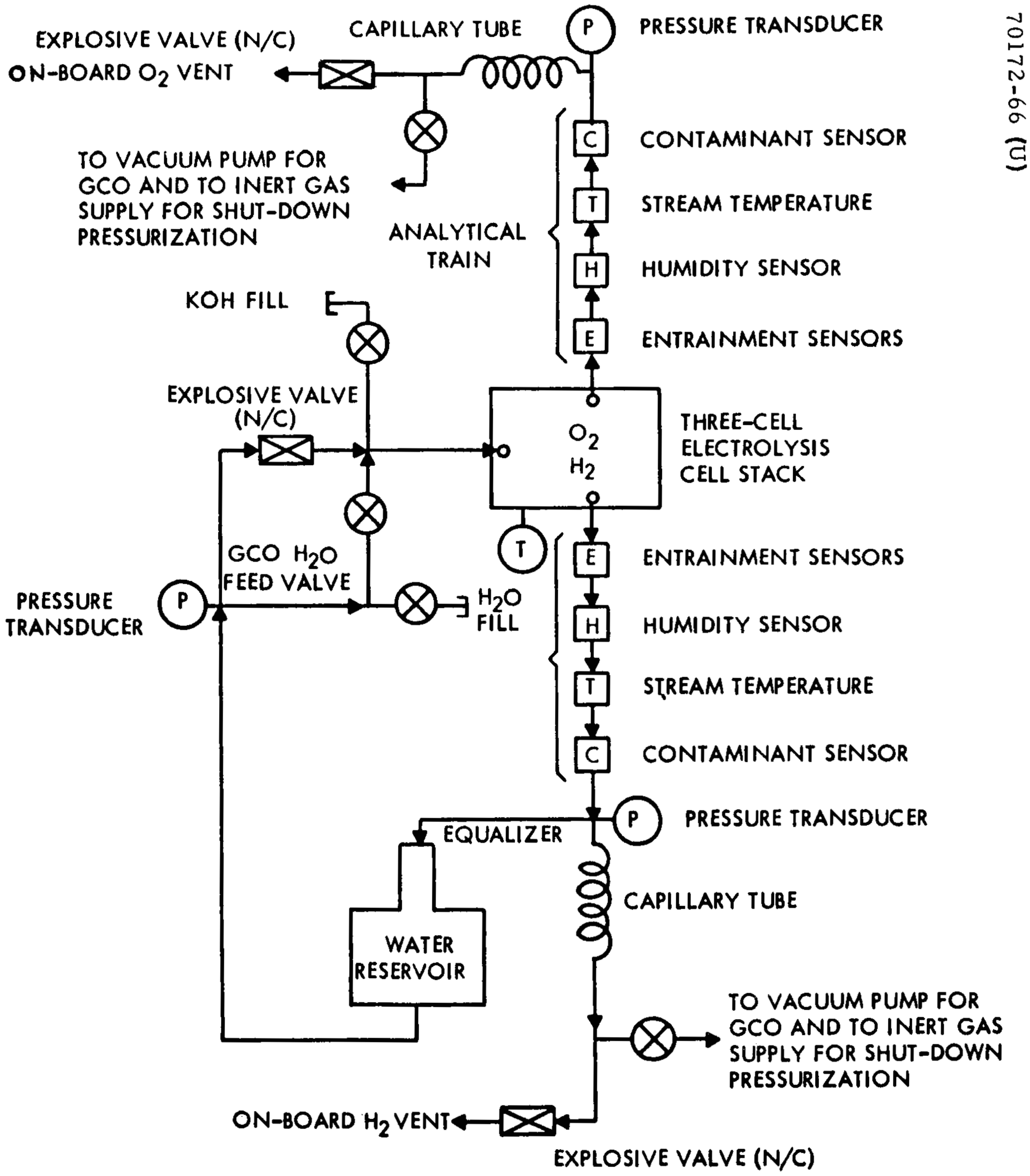

Figure J-8. TRW Flight Test Cell Block Diagram

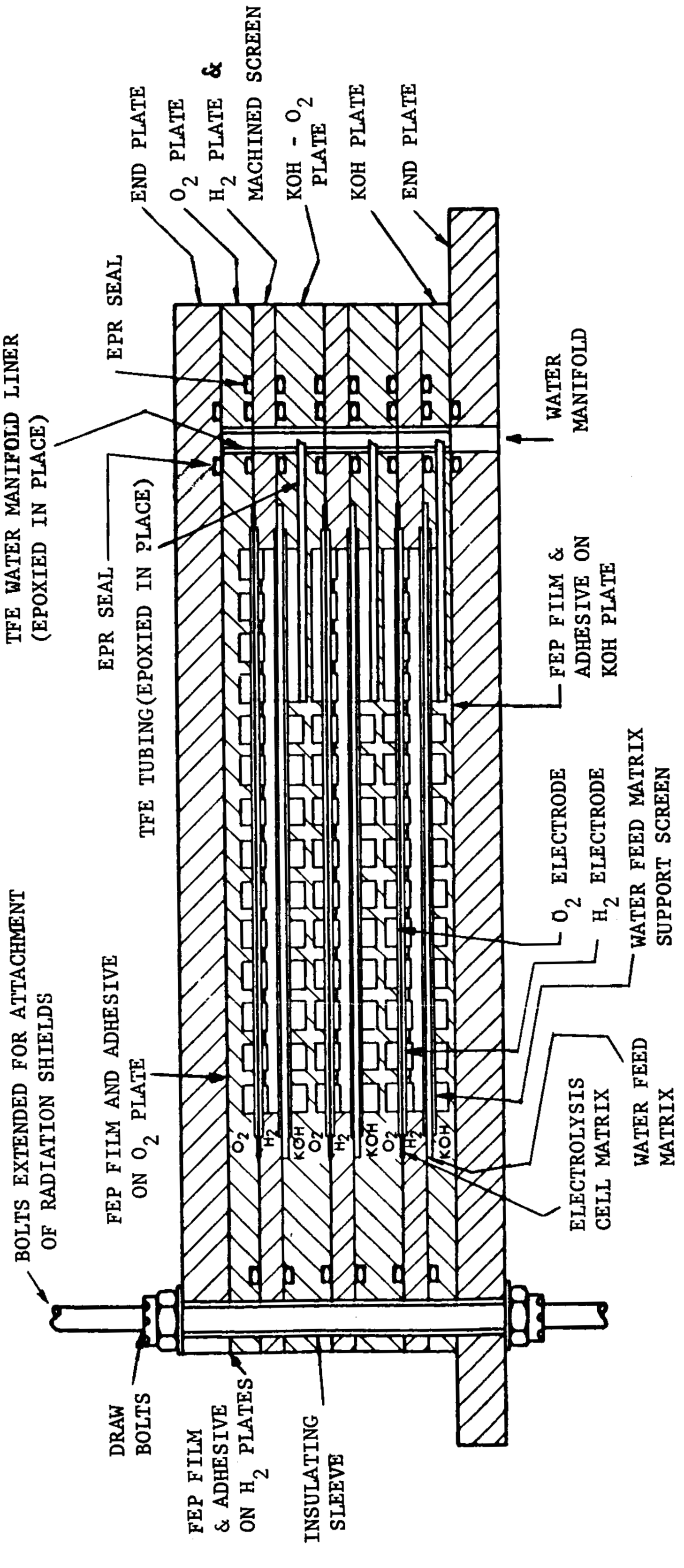

Figure J-9. TRW Electrolysis Cell Stack Cross Section

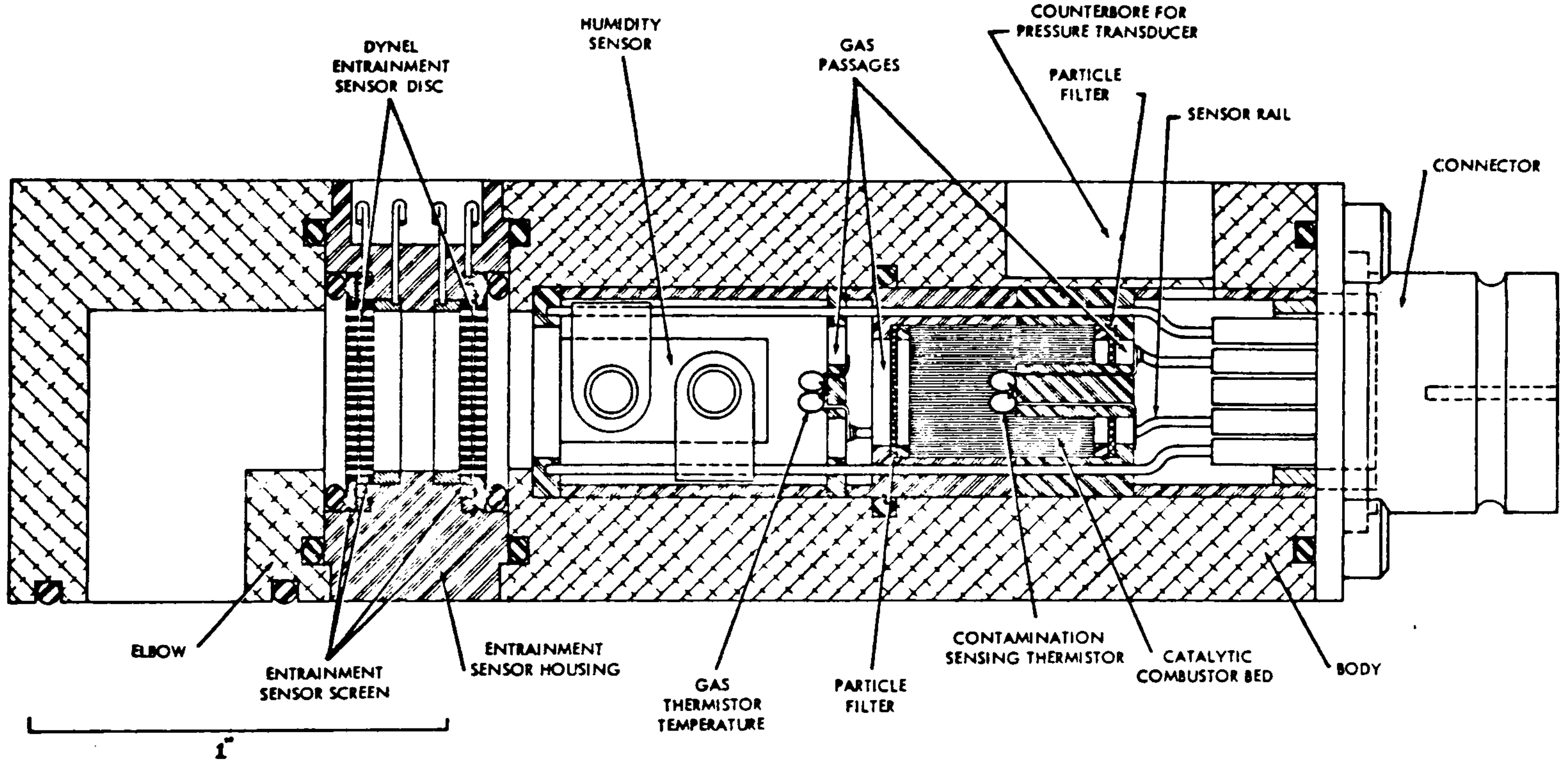

Figure J-10. TRW Cell Gas Analysis Train

Operating temperature	140°F
Hydrogen pressure	14.7 psia
Oxygen pressure	15.7 psia
Water pressure	13.7 to 11.2 psia
Cell voltage	1.57 volts
Stack voltage	4.71 volts
Water decomposition rate	0.139 lbs hr^{-1}

Although nominal operation is at a current density of 100 amperes ft^{-2}, the cell can be operated at current densities as high as 500 amperes ft^{-2}.

Sensors for the Flight Test System. The following sensors were selected for the flight cell:

1) Gas temperature: thermistor (Fenwall GB 32JM22, 2000 ohm).

2) Gas pressures: pressure transducer (Statham PA 208 TC-25-350 miniature strain gage type) to operate over range of 0 to 25 psia.

3) Cell current: dc current shunt (Janco 50 MV rated at 10 amperes (MIL-S-61B).

4) Humidity sensors: conducting filen type; unit selected was Phys-Chemical Research Corporation. Type PCRC-55.

5) Electrolyte entrainment sensors: cell composed of a 0.4-inch diameter Dynel M1450 fabric disk held between two platinum electrodes was constructed.

6) Gas contamination sensors: a sensor was constructed by wrapping American Cyanamid AB 6 electrode material around a thermistor; the extent of reaction was determined by the temperature rise of the catalyst-wrapped thermistor over an unwrapped thermistor.

7) Gas flow measurement: the pressure drop across a laminar flow tube of 0.010-inch internal diameter was used to determine the gas flow rate (exit pressure is space vacuum).

A diagram of the sensor assembly is shown in Figure J-10, which was taken from Reference 13.

Conclusions.

1) During the checkout tests on the flight hardware, changes in cell voltages and indications of possible leakage from the hydrogen system were observed. No explanation of these perturbations was found during subsequent examination of the hardware.

2) Performance evaluation tests on the TRW electrolysis cell are very limited. The reported acceptance test run of the prototype unit was under 25 hours in duration; tests of experimental cells were under 100 hours in duration.

3) Some of the materials employed in this cell may not be suitable for long duration missions. In particular, the selection of epoxy adhesive for holding insulating sheets and sleeves appears to be a possible source of difficulty where the joints may be in contact with hot, concentrated KOH. In the case of the sleeves in the water distribution passages, a breakdown of the insulation might result in electrolysis of the water in the water feed system (since the bipolar design is employed), resulting in the production of gases within the system which would disrupt the operation of the water feed system.

   The reliance upon pinhole-free gold plate for the protection of the magnesium structure from hot, concentrated KOH may not be justified for long-term exposure.

4) The "analytical train" of sensors developed under this contract seems to be a unique and useful method for evaluating cell performance.

5) While this particular design does not employ mechanical regulators for maintaining the proper pressure relationships within the cell, any useful application of this cell would require such regulation.

Allis-Chalmers Manufacturing Company

Three contracts for the development of water electrolysis cells are currently held by Allis-Chalmers (Reference 15):

1) The NASA/Langley Research Center ILSS cell (Contract NAS-1-6561), which is a four-man (9 pounds of water per day) unit to replace the General Electric cell in the Integrated Life Support System (ILSS) study at Langley.

2) An oxygen-generating cell for submarine life support systems (Contract NOBS-90502).

3) A cell for the U.S. Atomic Energy Commission Oak Ridge facility, which will generate hydrogen for ammonia production.

Allis-Chalmers base their electrolysis cell designs upon extensive experience with fuel cells and electrowinning devices. Over 600,000 hours of test cell operation are claimed. Contractual efforts for the development of fuel cells include contracts NAS-8-2692, NAS 8-20573, NAS-9-5834, AF-33(657)-89070, AF-33(615)-1185, AF-33(615)-3767, and AF-33(615)-3790. Additional research and development of fuel cells is also carried out with in-house efforts.

Experience at Allis-Chalmers indicates that the problem of obtaining materials of construction which will withstand long periods of exposure to the strong, hot alkaline solutions employed in their fuel cells and oxygen electrowinning apparatus has been solved. Since the electrolysis cells will operate at a lower temperature than the fuel cells, it appears likely that no additional material compatibility problems will be encountered in this development. Tests of greater than 15,000 hours (1.7 years) duration have been run in the electro-winning program.

ILSS Cell

Development work on this cell is being carried out under contract to NASA/Langley Research Center (NAS-1-6561). This work was begun in August 1966 with delivery of a completed prototype unit scheduled in August 1967. No formal reports on this development were available at the time of the survey. The information presented here was obtained in interviews with the principal investigators in November 1966.

The Allis-Chalmers ILSS cell is designed as a direct replacement of the General Electric ILSS cell and thus will be required to provide oxygen for a four-man crew (8 pounds of oxygen per day) on a 1-year mission, with possible resupply and maintenance at 90-day intervals. Oxygen purity of 99.999 percent or better is anticipated. The delivered unit is expected to weigh about 60 pounds and will be about 20 by 14 by 13 inches in size.

It is anticipated that the cell will be designed to operate at a current density of 100 amperes ft^{-2} at a cell voltage drop of 1.6 volts and a temperature of 175°F. Total parasitic power (power requirement in excess of the electrolysis current) is expected to be about 40 watts.

It is believed that the configuration of the cell will be similar to that of the 2-kilowatt fuel cell developed for NASA/Huntsville (NAS-8-2696). In this fuel cell the water is removed from the cell matrix by diffusion of the vapor to a water transport matrix. A net flow of water vapor in the direction of the water transport matrix results from the fact that the water vapor pressure at the water transport matrix is lower than at the cell matrix

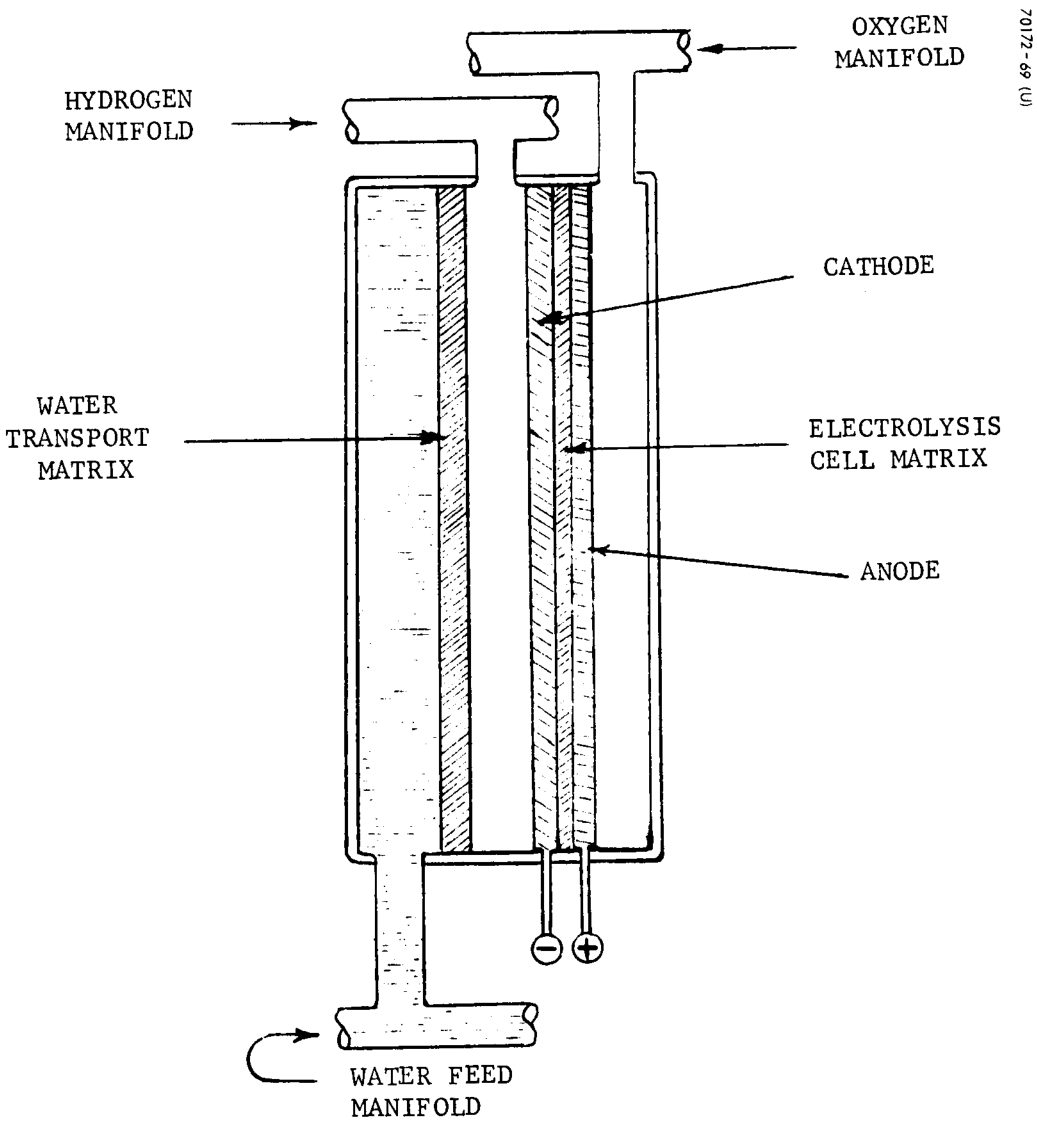

Figure J-11. Allis-Chalmers Electrolysis Cell Schematic because of a difference in the KOH concentrations at the two matrices. Water is removed from the water transport matrix by vapor diffusion into a region of still lower water vapor pressure; this pressure is controlled by a temperature-compensated regulator.

In the case of the electrolysis cell, it is anticipated that the arrangement will be similar to that in Figure J-11. Water will be supplied to the water transport matrix cavity; presumably the water pressure regulator or a check valve will prevent the loss of electrolyte. The KOH concentration at the water transport matrix will be approximately 30 to 35 percent (by weight), approximately 5 percent lower than the KOH concentration in the electrolysis matrix for an operating temperature of 175°F. The difference in KOH concentrations will provide for a net flow of water vapor to the electrolysis cell by diffusion. It is possible that some form of differential pressure regulation between the gas pressure and the water feed pressure will be required, but it is understood that such regulation can probably be avoided in the ILSS cell.

Electrolysis Unit for Submarines

Work was initiated in 1964 on a water electrolysis unit to provide breathing oxygen for submarine crews on a contract to the U.S. Navy Bureau of Ships (NOBS-90502). The units will be sized to deliver approximately 150 SCF per hour of oxygen at a pressure of 3000 psig. The development of these units is particularly interesting because of the long mission duration involved; it is anticipated that these units will operate for a number of years without major maintenance. Development tests of up to 10,000 hours have been carried out with no degradation of performance or materials of construction.

It is believed that the electrolysis units under development at Allis-Chalmers will be significant advancement over the present submarine cells, which are said to be a typical commercial type of design and require approximately 3.0 volts per cell. Comparison of this voltage drop with the 1.6 to 1.8 volts per cell required by typical Allis-Chalmers units indicates that power losses will be much lower for the Allis-Chalmers cell, resulting in a greatly reduced cooling problem and a much lighter unit.

Oak Ridge Electrolysis Cell

In a program for the AEC/Oak Ridge facility, Allis-Chalmers is developing an efficient cell for the production of hydrogen from the excess electrical power generated at that facility. As in the case of the Navy cell, the unit should be much more efficient than conventional commercial cells, even when operated at current densities an order of magnitude greater than the conventional cells.

Conclusions.

1) Of the three units described above, only the Langley ILSS unit is designed for operation under zero-gravity conditions. These cells have not been flight tested under zero-gravity conditions. The elctrolysis cell design is based upon the Allis-Chalmers fuel cell designs, however, which have a demonstrated capabilit for zero-gravity operation.

2) Of the companies surveyed here, Allis-Chalmers is thought to have the greatest background of experience in electrolysis cells, both in general and in the area of zero-gravity electrolysis cells. In particular, Allis-Chalmers is believed to have studied the effects of long duration operation to a greater extent than other companies. Electrolysis cell tests of over 10,000 hours duration, and oxygen electrowinning cell tests of over 15,000 hours duration have been run with negligible deterioration in the performance or the structure of the cells.

3) The voltage efficiency of the Allis-Chalmers cells is as high as, or higher than, reported efficiencies of competitive electrolysis cells, with the exception of cells employing a palladium-silver alloy cathode. The current densities of the Allis-Chalmers cells are also as high as, or higher than, competitive cells.

4) The design of the Allis-Chalmers zero-gravity cells is so arranged that nonvolatile impurities in the feed water will be left behind in the water transport matrix region; the electrolysis cell itself, which is fed by water vapor, will therefore be protected from poisoning by nonvolatile substances in the feed water. Long duration operation without significant degradation in cell performance therefore might be expected.

5) While the Allis-Chalmers cell design for the NASA/Langley ILSS study appears to be superior to alternative cells, the performance of this unit remains to be demonstrated experimentally.

6) A zero-gravity electrolysis unit is not available off-the-shelf at the present time. Such a unit in prototype form should be available in August 1967.

Lockheed Aircraft Company, Palo Alto, California

A study of water vapor electrolysis cells (Reference 16) employing sulfuric acid as the dehumidifier and electrolyte has been carried out by Lockheed Aircraft Company under contract NAS 2-2630. This effort roughly parallels the work done at Battelle on a phosphoric acid cell described here. A stream of the cabin atmosphere gases is directed through the cell, which removes part of the moisture and electrolyzes it, producing oxygen and hydrogen. The oxygen is added to the cabin atmosphere; the hydrogen is removed from the cell and either dumped or employed as a reducing agent for CO_2.

While considerable material is available on this cell, only a brief discussion is given because it is believed that cells which require the circulation of the gas stream over the electrodes, as this one does, are not practical for unmanned spacecraft.

Description of the Cells

A number of cell electrode materials and gelling agents for concentrated sulfuric acid were studied in this effort. The cell design ultimately recommended, however, is one in which the electrolyte (concentrated H_2SO_4) is circulated through the cell between the electrodes, and the gases are separated by a thin sheet of asbestos placed against the electrodes (see Figure J-12). An external heat exchanger is required to remove the waste heat from the process. Differential gas-liquid pressure regulation is required to maintain the liquid-vapor interface properly at the electrode.

The results of this study indicate that a cell supplying oxygen for one-man (2 pounds of oxygen per day) would operate at a voltage of 2.24 volts and a current density of about 100 amperes ft^{-2}, and would require 14 cells for a 28 volt electrical system. Unit weight would be about 19.7 pounds, unit volume would be about 0.19 ft^3, and total power required would be 284 watts.

Conclusions.

1) This type of unit is not suited to the mission considered here because of the requirement for a moving stream of gases over the cell. The comments made concerning vapor electrolysis cells in general under the discussion of the GARD cell also apply to this cell.

2) It is reported that in-house efforts in electrolysis cells for life support systems have been carried out at Lockheed (Reference 16).

Pratt and Whitney Division, United Aircraft Corporation

Pratt and Whitney has done in-house work on an intermittent basis over the past several years (Reference 17). The experimental cells appear to have been closely related to the fuel cells and were, in essence, fuel cells run backwards. Good performance as electrolysis cells was claimed. No contracts to support electrolysis work were reported.

Others

It is reported that Allison Division of General Motors Corporation, AirResearch Division of the Garrett Corporation, and Electro-Optical Systems, Incorporated have also carried out electrolysis studies. Detailed information was not available at this writing.

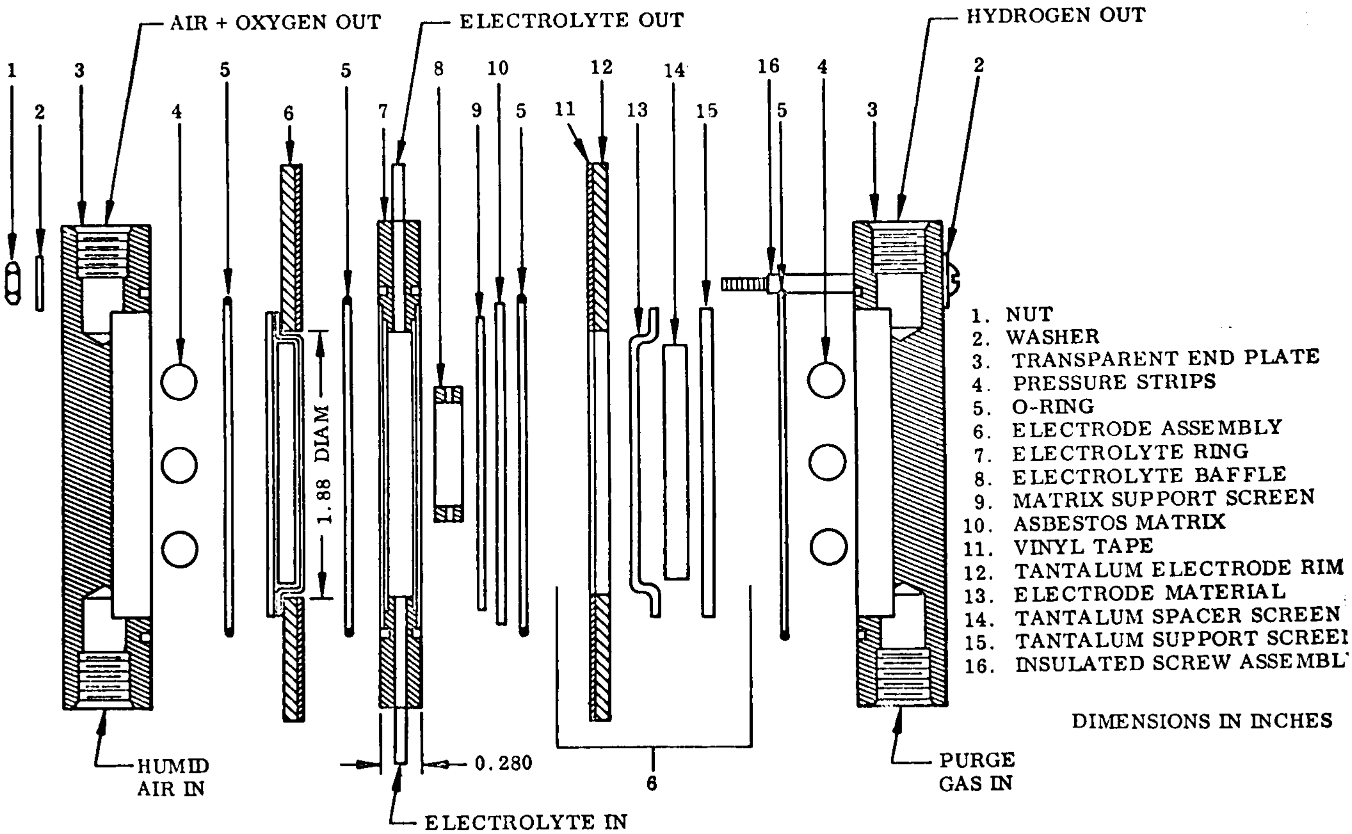

Figure J-12. Lockheed Liquid Electrolyte-Absorbant Matrix Cell Schematic

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